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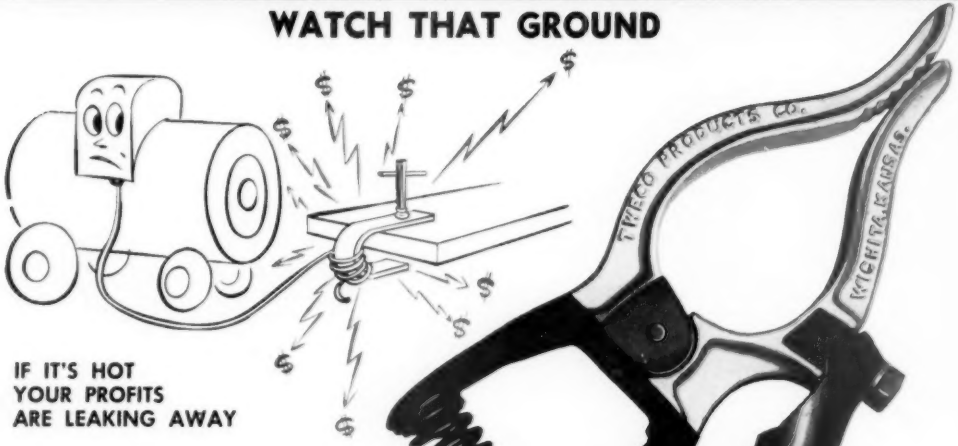

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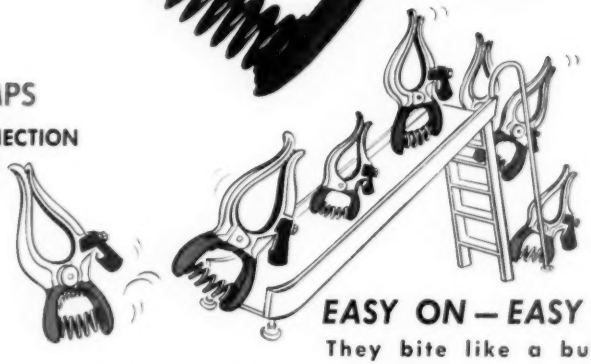

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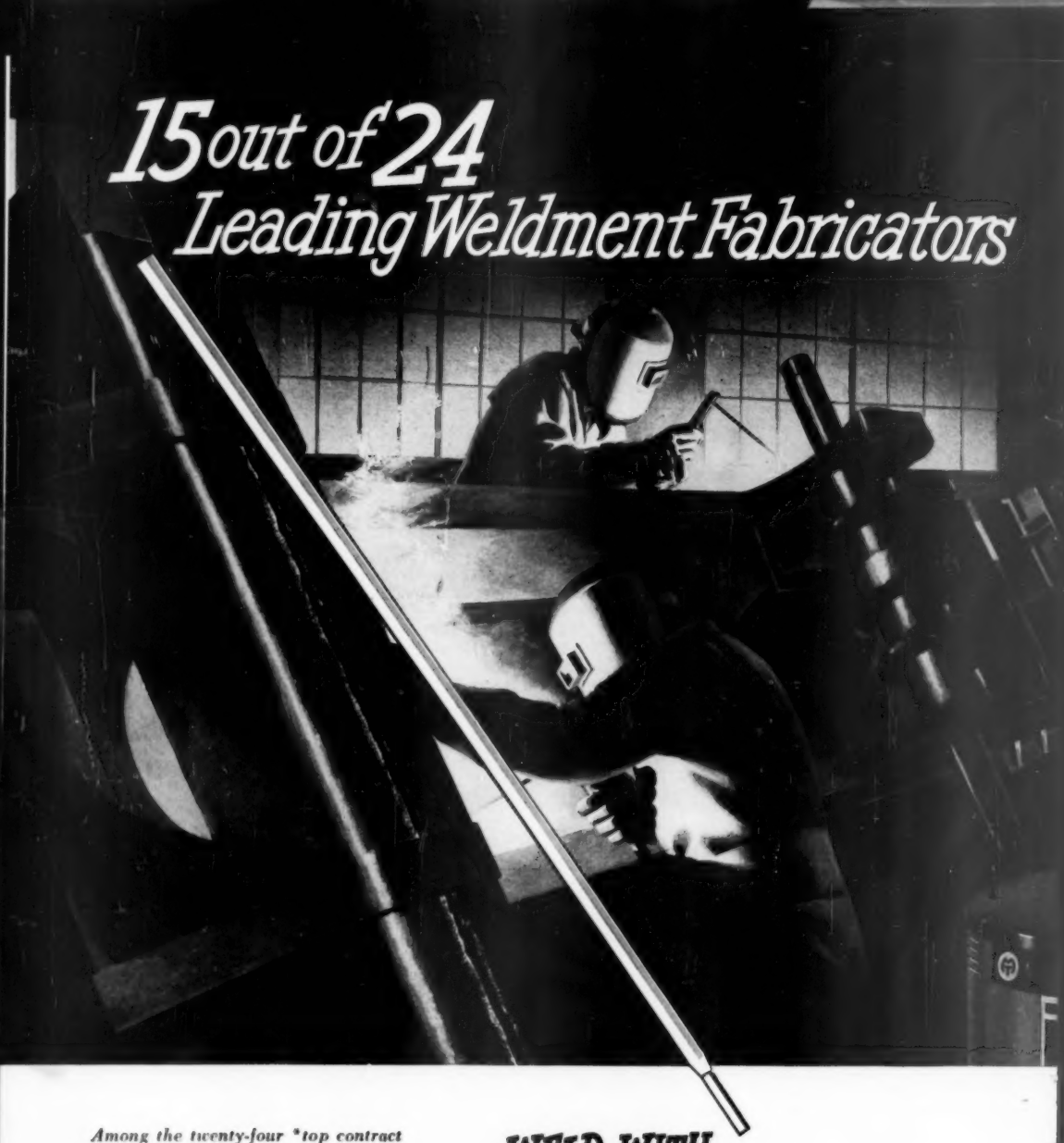

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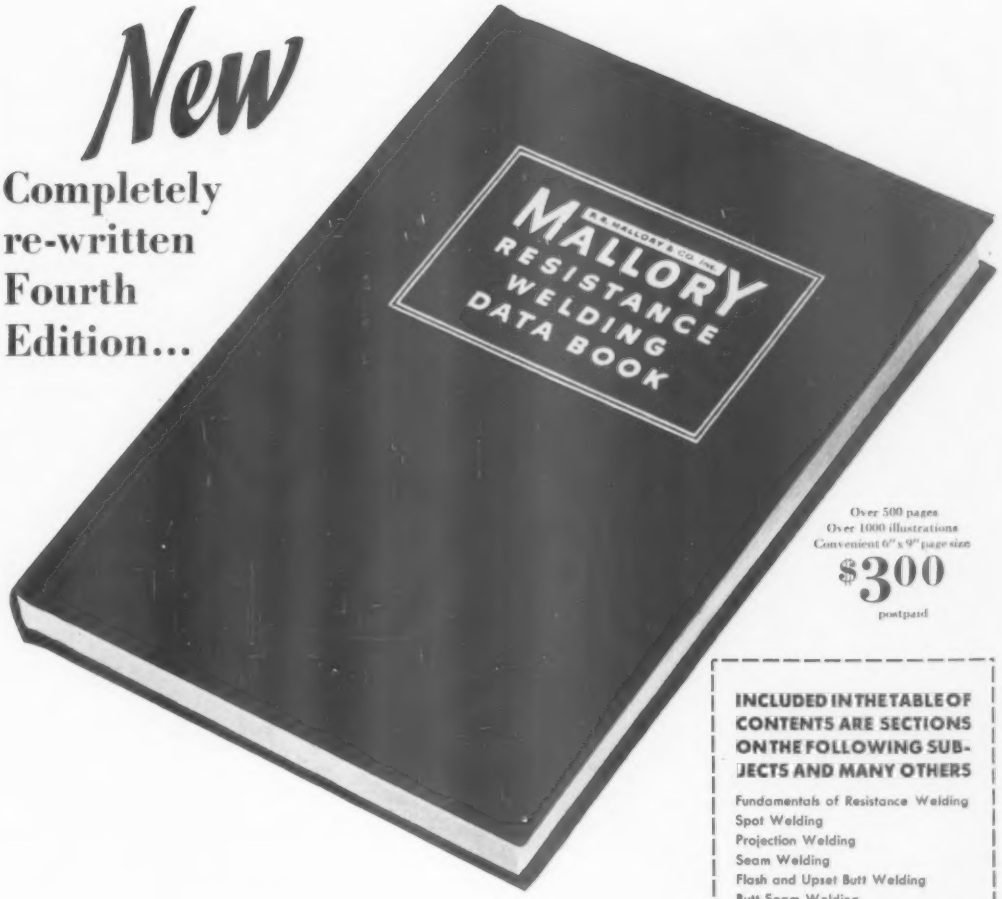

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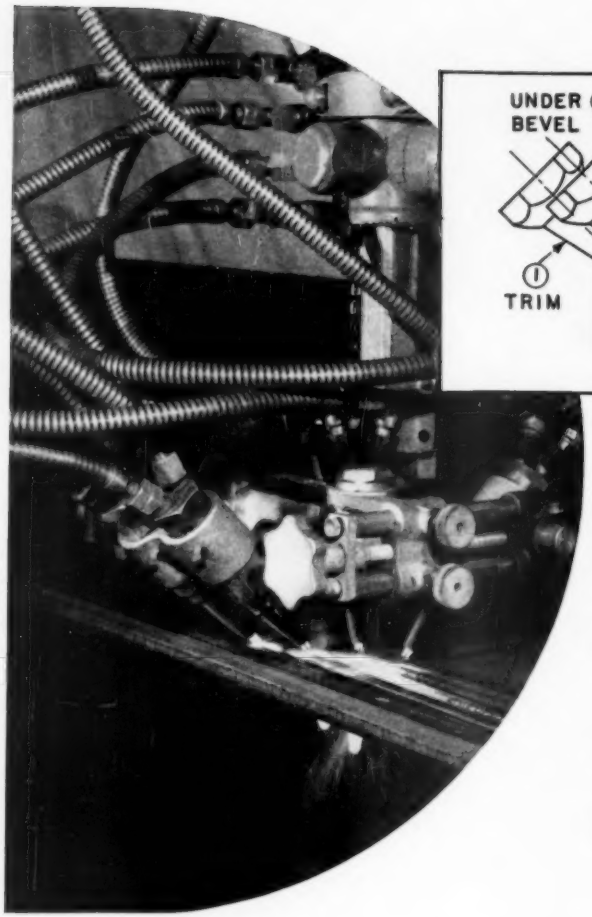

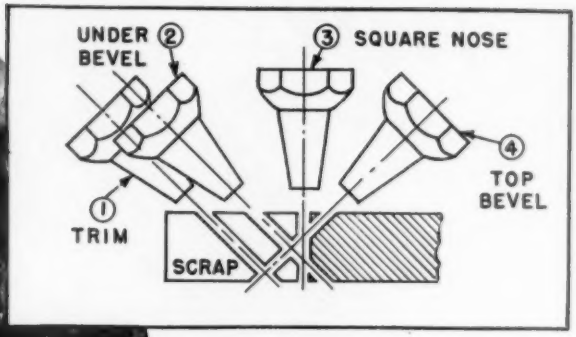

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The WELDING JOURNAL

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Control of Rail-End Hardening

Manual processes of flame hardening ends of ordinary carbon steel rail in the field using the compressed air-quench method and the self-quench method

by La Motte Grover

WHAT are the best methods for hardening rail ends in the field? Laboratory studies and field surveys of railway track are now providing the answer to this question. They are yielding information that puts field control of these heating and quenching operations on a more rational basis. Mystery and misunderstanding have prevailed for a number of years. Now metallurgical results can be predicted within practical limits.

Although there are still some differences of opinion regarding details of procedures, all investigators have found a great deal less batter for treated rails than for untreated ones, as well as longer joint-bar life and other benefits (*A.R.E.A. Proceedings, Bulletin 472,* **49,** 414). The principal lack of agreement now is on the degree and depth of hardness required for acceptable service behavior, and whether these are great enough to require the use of a compressed air quench.

Water quenching is prohibited by the proposed recommendations of Committee 4—Rail, of the American Railway Engineering Assn. (*A.R.E.A. Proceedings, Bulletin No. 458,* **47,** (February 1946)). In their program of rail investigations, they found that water quenching introduced difficulties in control to prevent overhardening and subsequent spalling, chipping or surface checking. Such defects have to be repaired by welding. Water quenching has usually entailed subsequent tempering which also is difficult to control in the field.

No reports have been found that indicate such surface cracking might result in fractures, but such defects occur soon after the hardening treatment and require earlier and more costly repairs. Therefore, in the absence of perfect control, it seems advisable to use procedures that may tend somewhat to provide less than optimum hardness, rather than risking overhardness. Even a moderate degree of overhardness in some cases has been found to result in more wear on surfaces adjacent to the hardened ends than at the ends. The relatively high rail ends then have to be ground down.

There is a dearth of accurate, recorded information that reflects the influence of rail end hardness upon actual service behavior under various intensities of traffic. Very few railroads have such records extending over a period of years. Unrecorded variations in procedures used from time to time introduce further difficulties in correlating them with results. Those who have supervised operations in the field have some rather definite opinions and convictions, but it is difficult to find recorded data to substantiate these impressions which are not always in agreement. One is confronted with somewhat of a dilemma in choosing the proper criterion for evaluating the results of a rail end hardening process.

There are some very useful data in the progress reports of the A.R.E.A. Committee, together with their proposed recommendations. One has the choice of adopting a process and procedure that will provide hardnesses and depths of heat penetration that conform to these proposed recommendations or, alternatively, to attempt to base his requirements upon the limited available data on amounts of batter in actual service, for rails end hardened by some of the various procedures that have been used.

Some railroad maintenance-of-way engineers feel that the latter alternative might lead to adoption of a procedure that involves no quenching other than the so-called "self quench" or "mass quench." This quenching is derived largely from the capacity of the adjacent cooler parts of a rail to conduct heat away

La Motte Grover is Welding Engineer with the Air Reduction, New York, N. Y.

Paper was presented at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

rapidly from the heated rail end surface. On the other hand, it seems to be impracticable to attain consistently the A.R.E.A. recommended hardnesses and depths of penetration without additional quenching from a forced air flow. Compressed air is often used to provide the quantity and rate of air flow required to produce specified hardnesses.

From a practical viewpoint, elimination of air quench equipment reduces greatly the weight and bulk of equipment to be carried. The transporting and handling of a heavy air compressor and auxiliary equipment become rather impracticable, especially when the work must be done on a single track line between train movements, as a maintenance operation.

In the absence of sufficient evidence to substantiate thoroughly the proposed A.R.E.A. Committee recommendations regarding desirable ranges of hardness, there is an increasing trend toward the use of the self-quench method.

Some confusion has resulted from the difficulty of making accurate, significant hardness determinations in the field. Although the presence of a decarburized surface layer is recognized in the A.R.E.A. proposed recommendations, it has been common practice to take hardness readings on a surface that has been ground only enough to get a good impression. Variations in depth and lack of homogeneity of the decarburized surface preclude a satisfactory correlation between surface hardness and the actual hardness below the decarburized layer, where the A.R.E.A. recommendations call for the specified hardnesses to be attained.

Partial decarburization has been found commonly to extend to depths as great as 0.025 in. (occasionally as great as 0.060 in.) below the surface of the rail, even after the flame-hardening operation has caused some diffusion of carbon to the rail surface. It is obvious that it would not be acceptable to grind through this layer on a rail in track for the purpose of getting accurate hardness determinations. When batter and wear, as measured $^1/_4$ in. from end of rail, become as great as $^1/_{32}$ in. (0.031 in.), some railroads consider it is time to build up the ends, this work being done as an out-of-face operation.

Experience indicates that it is preferable to heat treat the ends of rails when they are new, before the surface has been worn down somewhat by batter in service. From the practical viewpoint, also, this is usually more convenient. Laboratory studies have not indicated much influence from condition of rail surface before heating.

Air Reduction has been conducting a study and investigation of rail end hardening processes and procedures, in the field and in its research laboratories at Murray Hill, N. J. In the course of these studies, many of the fundamental conclusions of the A.R.E.A. investigators at the University of Illinois have been confirmed, and a great deal of additional information has been developed.

Fig. 1 A dual multiflame heating tip with two burners spaced $2^1/_2$ in. apart is used frequently for procedures involving a compressed air quench

The discussion of this paper will be confined to the manual processes of flame hardening ends of ordinary carbon steel rail in the field, Figs. 1 and 2, using the compressed air-quench method and the self-quench method. In these processes a substantial part of the quenching or cooling action is derived from the conduction of heat into adjacent cooler parts of the rail, even

Fig. 2 A comparatively large single-flame tip, operated in a shielding box, is used most commonly for self-quench procedures

though supplemented with a compressed air quench. For this reason, rapid concentrated heating with an oxyacetylene torch is universal practice in the field.

PROPOSED RECOMMENDATIONS OF A.R.E.A.

The proposed recommendations of the A.R.E.A. Committee, based upon laboratory testing and field observations are broad in scope, being designed to serve either mill or field practice. They are as follows:

"1. The area reheated for hardening should cover the full width of the rail head and extend longitudinally on the top surface between $1\frac{1}{2}$ and $3\frac{1}{2}$ in. from the end of the rail, and to a depth of not less than $\frac{1}{4}$ in. (*Author's Note*: This has been interpreted to mean a transformation depth of $\frac{1}{4}$ in.)

"2. Heating for hardening should be completed in a time not exceeding five minutes.

"3. Quenching should be performed by a process designed to avoid the formation of martensite. Water should not be employed as a quenching medium.

"4. Rails hardened in accordance with the instructions of Paragraph 1, shall show a Brinell hardness at a point $\frac{1}{4}$ to $\frac{1}{2}$ in. from the end, on a center line, beneath the decarburized surface, in accordance with the following:

Nominal weight per yard	*Permissible range in Brinell hardness*
90–120 lb.	331–401
121 lb. and over	341–415

"5. Care should be taken to avoid abrupt changes in hardness in the hardened zone."

EQUIPMENT AND PROCEDURES

For purposes of discussion two procedures will be described which are in common use and apparently are producing results that are considered satisfactory by the railroads using them. One of these involves a compressed air quench and the other relies solely upon the self quenching effect. If very loosely controlled, factors other than quenching may vary sufficiently to result even in somewhat less surface hardness with a compressed air-quench procedure than with a self-quench procedure. The self-quench method seems to require closer control than the compressed air method.

Some of these factors will be discussed later in this paper. With a reasonable amount of care exercised in following prescribed procedures, sufficiently accurate results can be predicted. Precision control is not required.

The heating equipment used in case of either procedure includes a medium size heating torch and suitable tips, a three-cylinder flexible hose acetylene manifold and suitable oxygen and acetylene regulators that will provide straight-line regulator performance to control both gas flows and gas ratio under the imposed conditions of repeated on-and-off torch operation. Fifty feet of $\frac{3}{8}$-in. acetylene hose and 50 ft. of $\frac{5}{16}$-in. oxygen hose are used for air-quench procedure. Other equipment and supplies include about 9 cylinders each of oxygen and acetylene, a surface grinder, a cross grinder and a shield to help protect signal bonds. This shield may be incorporated in a box that shields the flames from the wind and confines the heating to proper areas of the rail surfaces. A heavy-duty motor car is provided, or a suitably equipped tractor if off-track equipment is used, Fig. 3.

Fig. 3 A suitably equipped tractor carries supplies and apparatus when off-track equipment is used

A typical crew includes a torch operator, a helper to handle hose and apparatus, a grinder and grinder's helper, two flagmen and a motor car or tractor operator. If signal bonds have been installed and asbestos must be applied to protect them, this application and subsequent removal will require two laborers. In some cases, the labor cost for grinding would not properly be charged against flame-hardening operations.

Fig. 4 Air compressor and cylinders of oxygen and acetylene, mounted on heavy-duty push cars

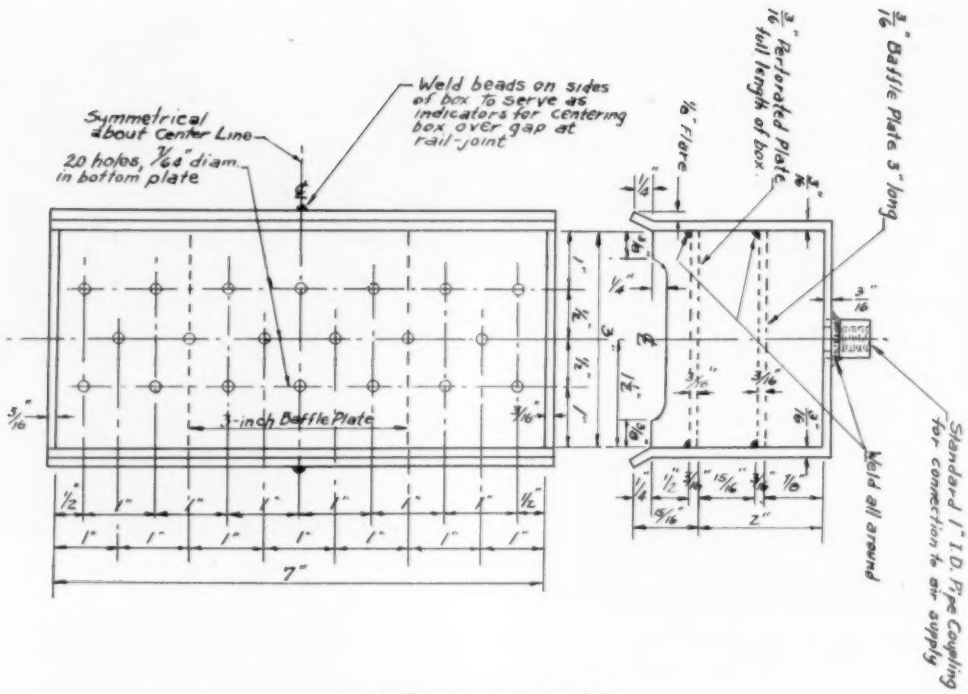
Symmetrical about Center Line
20 holes, 7/64" diam. in bottom plate
Weld beads on sides of box to serve as indicators for centering box over gap at rail-joint
3-inch Baffle Plate
3/16" Perforated Plate full length of box
3/16" Baffle Plate 3" long
1/16" Flare
Weld all around
Standard 1" I.D. Pipe Coupling for connection to air supply
7"

Fig. 5 Compressed air quench box

If a compressed air quench is used to attain hardnesses and depths of hardness that will conform strictly to A.R.E.A. proposed recommended practices, a portable air compressor of about 105 cfm. capacity is commonly used, suitably mounted upon a push car, Fig. 4. An air-quench box is provided to apply the air to the rail ends. In Figs. 5 and 6, a typical box is illustrated, with 20 holes or jets of 7/64 in. diameter in its bottom. The box covers a 7-in. length of rail. The perforated bottom sits 1/4 in. above the rail surface. Approximately fifty feet of 1-in. hose is used to carry the air to the quench box. An additional helper may be required to handle the quench box.

Air-quenching equipment of considerably smaller capacity has been used by some railroads usually with some sacrifice of attained hardness. Studies of the efficiency of various types of air-quenching equipment are being continued.

Since liquid water in the air supply has been found to exert an important and uncontrollable influence upon hardnesses attained, it is very important to remove such moisture, preferably by means of an air drier, such as a silica-jel drier. A liquid trap of some kind would be helpful if drained frequently. In any event before operations are started, the compressor should be run for several minutes with the drain cock on the air reservoir open, until the compressor warms up, to preclude the presence of liquid water in the air-quench line. Continuous bleeding is advisable during operation.

The general procedure is first to surface grind the rail ends at a joint to eliminate any difference in height and to cross slot the rail joint with a cross grinder.

Then the rail ends are heated as described later in this paper. If an air quench is used, it must be applied immediately after the heating. It is applied for not less than 20 seconds, with an initial pressure of about 40 to 50 psi. at the quench box. Full pressure is maintained for at least 20 seconds.

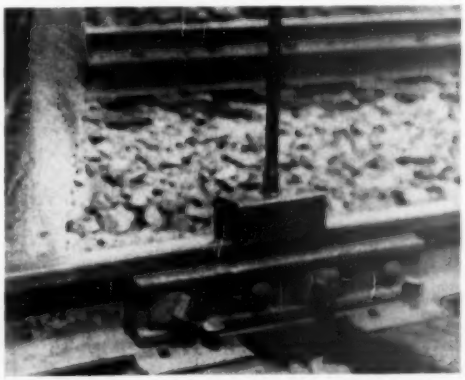

Fig. 6 When a compressed air quench is used, it is applied immediately after heating is finished

HARDNESS DETERMINATIONS

It has been customary to make occasional hardness readings on the rail surface after heat treatment, with only a slight amount of grinding to prepare the surface. However, in view of the decarburized layer on the surface and its lack of uniformity, such readings must be regarded as only a very rough check. If this method of checking is used at all, allowance must be made for the fact that such surface hardness values of BHN 269 to 341 have been measured where values determined for the underlying metal have fallen within the A.R.E.A. specified range of 341 to 415.

The only method that has been developed for making a reliable check of hardness is by the use of off-track dummy joints made with short pieces of rail, that are processed according to the standard procedure. At a point $^1/_2$ in. back from the end of the rail, about $^1/_{32}$ in. of metal is then removed slowly from the surface with a fine grinding wheel and low pressure, so that the hardness can be measured under the decarburized layer. With proper control over the rail end hardening procedure, only a very occasional check of this kind should be necessary and the expense should not be exorbitant. It has been found in the laboratory that Brinell values determined in this manner are in reasonable agreement with converted Rockwell values determined by interior exploration made on specimens obtained by sectioning the railhead.

If a portable Brinell tester is available it should be used in preference to a Shore Sceleroscope, because of the more meticulous procedure required with the Sceleroscope to obtain reliable readings. Also, it is impossible to make strictly accurate and significant conversions from Shore Sceleroscope readings to Brinell values.

HEATING PROCEDURES

For heating, both single-flame and multiple-flame burners have been used successfully. Sometimes a single tip is used, Fig. 2, oscillating it back and forth rapidly in heating the two rail ends of the joint in the same operation. Also, dual tips, Figs. 1 and 7 have been used with excellent results, with the two burners spaced $2^1/_2$ in. apart so that heat is delivered simultaneously to the two rail ends, but the tip is moved around at all times in a prescribed pattern.

Of course, it is very important to adhere to the procedure and heating time that have been established for the particular arrangement of apparatus. For example, if one were to change to the use of two burners, of the same size as specified for single-burner operation, and if the heating time were not altered accordingly and perhaps other features altered to compensate, it is obvious that the heat would be more concentrated and would be delivered more rapidly to the rail ends. In this way it would be possible to develop objectionably high surface hardnesses, probably of comparatively shallow depth, which could result in checking and spalling of the rail ends during subsequent service.

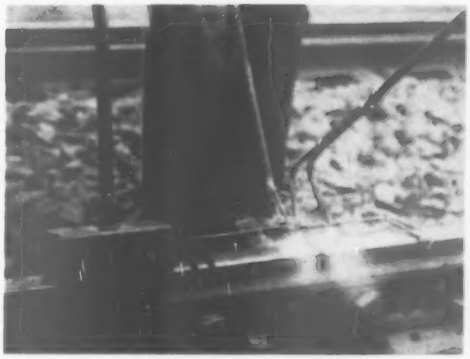

Fig. 7 Heating is done sometimes with a tip having two single-flame burners. Note use of shield to protect signal bonds

For one of the typical procedures that have been mentioned, which employs a compressed air quench, the heating is done with a dual tip of two burners spaced $2^1/_2$ in. apart, Fig. 8. Each burner has 9 holes of No. 60 drill size arranged in a $^7/_{32}$-in. circle (designated as a Style 739, No. 10 size tip). A spacer bar is clamped across the tip, providing two legs that slide on the rail surface. It is adjusted to provide a space of $^3/_4$ in. between the ends of the burners and the rail surface. This provides an acceptable distribution and depth of heating without requiring meticulous control to avoid overheating of the rail surface. A positive-pressure-type mixer, size 11 to 13, is used with this tip.

With 50 ft. of $^3/_8$ in. acetylene hose and a hose-entry (regulator) pressure of 11 psi., the desired flow of 150 cfh. of acetylene is obtained. This produces about $^1/_4$ in. depth of transformation in the railhead when

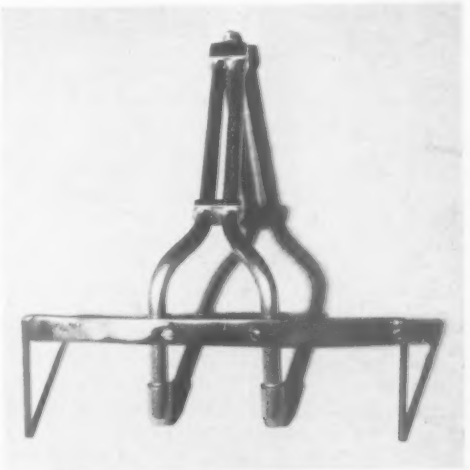

Fig. 8 A spacer bar with legs that slide on the rail surface insures proper tip-to-work spacing

the heating time is 60 sec. The acetylene consumed is therefore approximately $2^1/_2$ cu. ft. per rail joint during actual heating time. The flames are maintained neutral to slightly oxidizing. With careful control of the heating time, it might be cut down to 50 sec., but any lesser time, associated with this particular procedure would result in a lesser depth of transformation than the specified $^1/_4$ in. If the heating time is extended considerably, the depth of transformation may be increased further, but careful control must be exercised to avoid surface melting.

In heating, the full width of the rail head is covered uniformly for a distance of $2^1/_4$ in. on each rail end. One heating pattern that has been used, is a circle of $1^1/_2$-in. diameter on each rail end, with the edge of the circle about $^3/_4$ in. from the end and sides of the rail head. Another, rectangular pattern, developed in the laboratory, seems to facilitate the proper distribution of heat, and it produces uniform hardness patterns. A rectangular path is followed on each rail end with its edges $^1/_4$ to $^3/_8$ in. from end and edges of the rail, with an occasional figure-eight motion.

During approximately the last 15 sec. of heating, after color appears, the heat is spread across both rail ends and is extended to the edges of the rail, using longitudinal strokes and manipulating the torch to produce a uniform color. Immediately after heating the compressed air quench is applied.

For the other typical procedure, which has been referred to as the self-quench method, a single flame tip of larger size has been used most commonly, oscillated back and forth from one rail end of the joint to the other. From the discussion of heating characteristics which follows later in this paper, it would appear that a dual, multiflame tip of suitable size might be used to good advantage for this procedure as well as for the compressed air-quench procedure. However, the procedure as used most commonly will be described.

The torch is provided with a semi-injector type mixer (811-1415), and a straight 12-in. extension.

The single-flame tip is known as a Style 8, which produces a comparatively long flame. The size used, No. 13 has an opening of No. 10 drill size. With 100 ft. of $^3/_8$-in. acetylene hose leading to a quick-acting valve block and 5 ft. of $^5/_{16}$-in. acetylene hose from the valve block to the torch, an acetylene hose-entry pressure of $11^1/_2$ psi. is used, as measured while the gas is flowing (approximately 15 psi. when not flowing). The torch entry pressure would be about $6^1/_2$ psi. for an acetylene flow of 250 cfh. or $4^1/_2$ psi. for 200 cfh. About 40 psi. oxygen pressure should be sufficient, if the same hose-sizes are used for the oxygen.

The recommended practice is to adjust the oxygen regulator to produce a neutral flame, while both torch valves are wide open and the $11^1/_2$ psi. acetylene pressure is maintained. For accurate control of gas flow the acetylene valve must not be throttled.

Several patterns of heating or torch manipulation have been used. It is very important to administer and distribute the heat properly, to avoid excess or insufficient hardness and provide the desired distribution of hardness and depth of transformation. This is discussed later in more detail.

One technique is to heat 3 or 4 sec. alternately on each rail end in a circular path such as one of the patterns described for the compressed air-quench method, continuing this technique for about 15 to 20 sec. Then the flame is oscillated back and forth over both rail ends for an additional 12 to 15 sec., with longitudinal strokes, spreading the heat over the entire width of the ball of the rail, to produce uniform color. The total heating time is about 30 sec. or slightly longer.

With another arrangement, a dual or twin tip with two single-flame burners or tips spaced $2^1/_2$ in. apart is used, one burner for each rail end, each burner having an opening of No. 20 drill size. The technique of manipulation is similar to that described for the compressed air-quench method, but the total time for heating is only 30 to 35 sec. when the air quench is omitted and the size of burners increased to the No. 20 drill size.

An accurate timing device should be used with any of these procedures.

The torch is held so that the extreme end of the visible cone of the flame does not quite touch the rail surface. A spacing device should preferably be used to insure such a tip-to-work spacing.

In the absence of any quench other than the conduction of heat by adjacent parts of the rail and by the relatively still air of the atmosphere, it is particularly desirable to restrict the heating to the intended areas and keep the adjacent parts as cool as possible. To this end, the heating is done in a steel box-like enclosure, Fig. 2, provided with fire bricks or masses of metal, to confine the flame to the surface areas being heated and to help maintain secondary flame envelopment. Fire bricks, suitably located, protect the material of the box from the intense heat.

Laboratory studies have led to the recommendation of some kind of wind break when wind velocities exceed 20 mph. The above-described box will serve also in this capacity, when needed, to insure that the flames of the heating torch are not displaced sufficiently to result in unsymmetrical heating of the surface of the rail and unsuitable distribution and degree of hardness.

For this self-quench method the correct time interval during heating is especially important. An increase or decrease of only 10 or 15 sec. from the optimum heating period has been found to decrease the hardening so much as to make the operation virtually worthless. Sometimes such an increase will cause surface melting.

In some procedures that have been tried for this self-quench method, more of a flash heat has been used than that just described. It should be kept in mind that very high rates of heating for comparatively short intervals of time tend to produce very shallow depths of hardness and perhaps surface melting.

The practice on some railroads has been to heat a somewhat greater length of rail end on the gage side of the rail, where the wear is greater and there are no signal bonds to be protected from the heat. This is ac-

complished by using longer strokes of the torch on the gage side, while the heating is being spread and equalized over both rail ends with longitudinal strokes as has been described.

INFLUENCE OF VARIABLE CONDITIONS AND PROCEDURE ADJUSTMENTS

To exercise proper control over the process it is important to recognize the variable factors or conditions and their relative importance in influencing the results. There are at least some twenty-five variables that might conceivably have an influence. These were studied at the Air Reduction Laboratory by making a limited number of deviations from a basic procedure that had been developed in the field, and by supplementing these studies with fundamental data to substantiate observations of effects and trends.

A reliable check of hardness requires the use of off-track dummy joints as has been mentioned. This would be expensive if used more than for an occasional check. Further, it is well recognized that with variable conditions of outdoor light, color observation of the heated rail is of no value except as an indication of uniformity of heating. Therefore, the most effective and least expensive method of control is to follow conscientiously an established procedure. An occasional hardness check should reveal any inadvertent deviations.

The procedure to be used must be developed for a specific group of apparatus and equipment, including torch, mixer, tip, regulators, size and length of hose and specific equipment for compressed air quenching, if used. Also, it must be predicated upon specific conditions of operation such as gas pressures, flame characteristics of the heating apparatus, heating time, tip-to-work spacing, torch manipulation, compressed air pressures and minimum quenching time.

These variables can all be controlled quite easily within the required limits by reasonable care in operation and by maintaining apparatus, hoses and their couplings, splices and glands, in good repair and proper adjustment. Gas cylinders should not be used below the regulator end point. Other correct practices should be followed which are commonly recognized for the operation of such oxyacetylene apparatus.

Other variable factors, which cannot be controlled by the operators of the equipment, have been studied at the Laboratory, with special reference to the compressed air-quench method. Their influences have been evaluated, so that the procedure can be varied to compensate for them when necessary, in sufficiently extreme cases. The effects of some of these variables, of both kinds, controllable and uncontrollable, will be discussed.

Carbon Content of Rail

The A.R.E.A. investigators showed that for one of the processes covered by their work, involving a compressed air quench (see Appendix), an increase of carbon from 0.71% to 0.80% increased the hardness a rather small amount from Rc 33 to about Rc 35 (from about 311 BHN to 331 BHN). The range of carbon specified in A.S.T.M. Specification A1–49T for 121-lb. rail and over is 0.69 to 0.82%, only a little greater than the range investigated. The permissible range in hardness for treated ends of such rail sections, as recommended by the A.R.E.A. Committee, is 341 to 415 BHN (under the decarburized surface), a much greater range than that mentioned above. Therefore, it does not appear that variations in chemical composition of the rail should cause any great difficulty in maintaining adequate control.

Rail Section

The procedures developed were based upon a 131-lb. rail section. The same heat input used for lesser widths of rail head would merely tend toward somewhat deeper transformation depths that would not appear to be objectionable. Therefore, when the compressed air quench method is used, no need is anticipated for procedure adjustments for other rail sections that are likely to be end hardened. With the self-quench method, the heating interval is sometimes decreased about five seconds for somewhat lighter rail sections such as 115 lb., as compared with that for 131-lb. rail.

Rail Gap

In the A.R.E.A. investigation, laboratory rail batter tests showed no significant change in resistance to batter, for variations of gap between rail ends from 0 to $\frac{1}{4}$ in., when the hardness as measured 0.03 in. below the natural surface was not less than Rc 35 (BHN 325).

Initial Rail Temperature

The basic air-quench procedure, as described in this paper, was developed for an initial rail temperature of 70° F. From basic considerations it can be shown that deviations of as much as 70° in either direction from the assumed 70° will produce comparatively small differences in the final temperature reached at any significant depth below the surface. Deviations as great as to 32 or 140° can be compensated for by increasing or decreasing the heating time by a few seconds. This, in turn, can be resolved into a variation in depth of transformation. Tests at the Airco Laboratory confirmed a conclusion that a variation of less than plus or minus $\frac{1}{32}$ in. of depth of transformation would result from such variations of initial rail temperature. This included the effect of initial temperature upon quenching rate. It was concluded that to compensate for extreme conditions of initial temperature of 32 or 140°, the heating time should be increased or decreased 5 sec., or alternatively the acetylene flow increased or decreased about 10 cfh. This would be accomplished by a change in acetylene pressure of plus or minus 1 psi, in the case of the apparatus and procedure that have been outlined for use with a compressed air quench.

Weather Conditions

Various wind conditions were simulated in the Laboratory while specimens were heated, and subsequently hardness explorations were made. As was to be expected, a substantial displacement of the secondary enveloping flame resulted in lack of symmetry of the hardness pattern, as well as a reduction in the depth of transformation. It was recommended that when wind velocities exceed 20 mph. (moderate to fresh) some kind of wind break be provided. The kind of shielding box for heating operations that has been described previously helps to shield the flame from the wind. To protect the operator, also, and prevent an influence upon his manipulation of the torch, some kind of canvass, wood or sheet metal wind break would no doubt be helpful, as in the case of welding in windy locations.

If the work must be carried out during rainy periods the control of the work would surely be facilitated by providing some kind of canopy to keep the rain off the operator as well as the rail ends being treated.

When quenching air is provided by a compressor, the presence of liquid water or mist (as opposed to water vapor) in the stream of quenching air can produce high quench rates and excessive hardnesses. This has resulted in so-called "weeping cracks" and spalling and chipping during subsequent service. During periods of rainy weather or high humidity there would, of course, be more tendency for moisture to precipitate and collect in the air reservoirs and air lines, unless precautions are taken to prevent it. Liquid water in the air stream also produces nonuniform hardness with considerably higher values directly under the air jets. These adverse conditions seem to be more pronounced when shallow, flash heating is used.

There is no doubt that positive moisture control should be provided and is needed under many of the prevailing conditions of weather and atmosphere that are encountered.

Flame Characteristics, Heating Time and Manipulation of Torch

A limited study was made of the effects of various types of tips, flame characteristics and manipulation or heating pattern.

It appeared that for multiflame tips, while primary flame mass was important, it was not critical as to disposition, as long as it acted as a mass rather than as individual flames rather widely spread. Envelopment of the rail surface by the secondary flame appeared to be important to prevent heat losses during the heating interval.

With a single long-flame tip, consuming 150 chf. of acetylene, for each rail end, a transformation temperature at a point $^1/_8$ in. below the rail surface was reached in about 31 sec.

With the use of smaller capacity multiflame tips providing 75 cfh. of acetylene for each rail end (half as much total acetylene consumption per joint), the heating time for transformation temperature at the same depth was increased, but only to some 40 sec. in the case of the dual style 739, size No. 10 tip that has been described.

With some unavoidable variations in other factors, a heating time of only 40 sec. may produce very little depth of hardness. A depth of transformation conforming to the A.R.E.A. recommendations was attained by extending the heating time with this dual multiflame tip to 50 or 60 sec., without risk of overheating the rail surface. A heating time of 90 sec. tended to cause melting of the rail surface which can lead to spalling and chipping in service.

This style of dual, multiflame tip, manipulated as has been described, so as to deliver a series of impulses in rapid succession, with secondary flame envelopment all the while, appears to be very well suited for the desired distribution of heat. This opinion is based upon fundamentals of variable heat flow as well as practical experience.

The type of heating tip and the manipulation should be chosen to provide as efficient heat transfer as practical, and to cover the desired area, at the same time providing at least the minimum permissible depth of transformation. Apparently, the best results are obtained by a compromise. Full rail-end coverage with continuous steady heating, on the one hand, would result in the most efficient heat transfer but inadequate penetration. On the other hand, an oscillation back and forth between rail ends, with impulses delivered too far apart, would result in deeper penetration than required, at a considerable sacrifice in efficiency of heat transfer.

Although an oxidizing flame of say 20% excess oxygen was found to increase the depth of heat penetration somewhat, it is rather difficult to control oxidizing gas ratios accurately in a field operation. The appearance of the flame is not as good a criterion as in the case of neutral or carburizing flames. Therefore the use of a neutral to slightly oxidizing flame is recommended, favoring the latter.

A spacing of $^3/_4$ in. from end of tip to surface of rail was chosen for the Style 739, No. 10 multiflame tip, because little difference was observed in hardnesses or depths of transformation for $^1/_2$ and $^3/_4$ in. spacings. The greater spacing eliminates need for critical control to prevent overheating of the rail surface.

Character of Air Quench

The equipment and procedure for the compressed air quench, as described in this paper, were developed to provide hardnesses and depths of transformation that would meet consistently the requirements of the A.R.E.A. proposed recommendations, without the need for critical control such as would be difficult to exercise in the field. The study of air-quenching apparatus is being continued to see if it is possible to develop a somewhat more convenient and equally efficient system. The studies made have yielded interesting information

upon the effectiveness of various amounts, velocities and characteristics of air flow through the orifices of the quench box of Figs. 5 and 6.

The air velocity, contact area and air-stream expansion are known to be influential factors, as well as presence of moisture in the form of liquid water. Since the latter cannot be controlled as to degree, all liquid water should be removed as has been mentioned.

Tentative recommendations have been based upon an air pressure of about 40 psi. at the entry to the 50 ft. of 1-in. hose leading to the box. This produces an air flow of 100 cfm. For this rate of air flow no excess hardness nor martensite was developed. A considerably higher rate of air flow, 183 cfm. obtained by maintaining the hose entry pressure at 90 psi., produced objectionably high interior local hardnesses, in many cases. In some cases it produced very high corner hardnesses at the very end of the rail, due to the air contacting both the top and end of the rail. The points of local, interior, excessive hardnesses coincided with the jet locations of the air box.

Tests made with lesser air flows indicated that if the flow should fall off to as low as 75 cfm. or even somewhat less, sufficient hardness would be produced in connection with the method of treatment that has been outlined in this paper. For good control the air should be flowing when the air box is applied to the rail.

With air-quench equipment as described for this method, the 40-psi. hose entry pressure producing a flow of 100 cfm. is equivalent to a pressure of 23 psi. at a pressure tap on the air box.

Although immediate application of the air quench after heating is recommended, a 2-sec. delay could be tolerated. The effective action of the quench does not occur until a few seconds after heating, when the rail surface has cooled to the transformation temperature range.

Fundamental considerations lead to the belief that within the limits to be encountered in field practice, the temperature of the air used for quenching would not be a factor of concern.

Other variables such as decarburization of the rail surface have been discussed in appropriate parts of this paper. For convenience of an operator, a number of important variables are listed in Table 1, with individual deviations that have been found to result in a change in depth of transformation of about $^1/_{32}$ in. It appears that these variables are largely independent of each other in their influence upon depth of transformation.

Included in the appendix to this paper are such rather meager data as have been found on end flow, batter and work hardening, under actual service of rail in track. Apart from the sources of these data, many railroads were contacted recently in a field survey, some of them using the compressed air quench method and others using the self-quench method. Many of them who had no records of actual data, reported that they had made careful periodic inspections on end-hardened rail that had been in service for periods as great as 10 or 12 yr. They had found entirely satisfactory results when a reasonable amount of control had been exercised in applying the process, following procedures similar to those that have been outlined in this paper.

Table 1—Individual Deviations That Will Result in Approximately $^1/_{32}$-In. Variation of Transformation Depth

(This table pertains to procedure described for use with compressed-air quench)

Item	*Basic procedure*	*Deviation*	*Working range*
Time, sec.	60	±5	55–65
Acetylene flow, cfh.	150	±10	140–160
Acetylene pressure, psi.*	11	±1	10–12
Spacing, tip-to-work, in.	$^3/_4$	±$^1/_8$	$^5/_8$–$^7/_8$
Ratio, oxygen to acetylene	Neutral	−0 to +20%	Neutral to 1.20:1
Initial rail temperature, ° F.	70	−40 to +70	30–140

* Pressures are based upon apparatus as described and 50 ft. of $^3/_8$-in. size acetylene hose. Corrections may be necessary for other conditions such as size, length and condition of hose.

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Appendix

The field survey data of the A.R.E.A. Committee Report extends over a total period of six years. Unfortunately, for use in this study, none of the procedures employed in the A.R.E.A. investigation conforms very closely to either of the two most popular and apparently successful procedures in current use today for field operations of rail-end hardening as described in this paper. Much shorter actual service records have been found and will be given for field procedures similar to those in current use.

Only three of the methods used in the A.R.E.A. investigation are of interest for comparison in connection with manual processes carried out in the field. Since their work included no manual method using an air quench, their Process No. 2, a mill treatment, involving comparatively long heating and air-quenching intervals, provides the nearest comparison to the air-quench method used in the field.

A self-quench field method was used, but the heating apparatus and procedure differ from manual methods commonly used in the field at present. However, the essential features of more rapid heat input, shorter heat-

Table 2—Average Batter in Thousandths of an Inch

(Measured 1/4 in. from end of rail on center line (A.R.E.A. Investigation))

Date	*June 1939*	*April 1940*	*April 1941*	*April 1942*	*April 1943*	*May 1944*	*May 1945*
Traffic, millions of tons	1.85	38.0	77.0	125.0	174.0	222.5	267.3
Self-quench process	6.8	8.0	7.1	7.5	9.3	9.6	11.9
Water-quench process	5.9	8.0	8.6	11.3	14.2	16.0	17.9
Air-quench process	6.7	7.5	7.3	8.5	9.5	10.1	11.8
Unhardened	9.8	13.0	12.0	13.1	15.0	16.1	22.0

ing interval and self-quenching in still air are represented.

The three above-mentioned procedures used by A.R.E.A. are briefly described in the Proceedings as follows:

"*PROCESS 2, MILL E. Gas Heating. Compressed Air Quench.* End of rail head reheated in a portable refractory lined furnace with gas flame to approximately 1550 deg. F. in 3 min. Quenched with jet of compressed air for 3 min.

"*PROCESS 7, Self Quench Field Process. Oxyacetylene Heating.* Both rail ends of a joint are heated at the same time by three oxyacetylene tips over each rail. The surface of the rail is heated to 1550 deg. F. in 18 to 24 sec. Quench is by conduction of the cold metal under the heated zone.

"*PROCESS 9. Free-Held Oxyacetylene Torch. Water Quench.* Both rail ends of a joint are slowly heated to 1500 deg. F. with free held oxyacetylene torch, then quenched to 300 deg. F. with warm water from a sprinkling can with small holes. After quenching, the hardened area is tempered by reheating to 650 deg. F. and allowed to cool in air."

These processes will be listed in the accompanying tables simply as air quench, self quench and water quench. Included also are comparative data for untreated rail.

The A.R.E.A. test track was on the Chesapeake & Ohio Railway near Carey, Ohio. Rail-end batter measurements were taken each year for a point 1/4 in. from the end of the rail, following the usual procedure with a 48-in. batter gage. The measurements tabulated give the extent of relative wear below adjacent surfaces existing at the time of measurement. Table 2 gives a summary of the average amounts of batter measured annually for each process.

Profiles were plotted and reproduced in the *A.R.E.A. Proceedings*, **47**, 471 (1946). Extending 24 in. in each direction from the middle of each rail joint they show that some differences in elevation exist between the leaving and receiving rail ends—as great as four to eleven thousands of an inch. For the self-quench group and the unhardened group the transition from the general elevation of the leaving rail end to that of the receiving rail end is considerably more abrupt than is the case for the air-quench and water-quench groups. Table 2 shows that at the end of six years, the localized batter as measured 1/4 in. from rail end is substantially the same for the self-quench and air-quench methods.

If riding characteristics of the rail, and service life up to the time when ends must be rebuilt by welding or joint bars repaired, are influenced principally by the amount of highly localized batter (only 1/4 in. from the end of the rail), then there would appear to be little difference in the effectiveness of the particular self-quench and air-quench processes that were used in the A.R.E.A. investigation. On the other hand, if the general profile across the joint, for a distance of about 4 in. in each direction from the rail ends is the more important criterion, the results of the air-quench method would seem to be quite substantially superior.

More recent data have been taken by representatives of Air Reduction on a stretch of 28 miles of new 115-lb. main line track, with half of the joints hardened by the compressed-air method and the other half by the self-quench method, using procedures that are similar to those described in this paper as typical of current practice. As yet, results after one year of service under about 11,000,000 tons of traffic are all that are available. None of the rail was surface-ground prior to hardening. All joints were chamfered within a month after hardening. Some of the joints hardened by the self-quench method show a slight amount of end flow. Those hardened by the compressed air method show no end flow.

The average end batter measured for 25 of these joints hardened by each process is substantially the same for both processes, about 0.012 in. The average hardness after the work hardening and wear of the one year is 331 BHN for the self-quench method and 358 BHN for the compressed-air method. The average increase in surface hardness during the year was about

Table 3—Surface Hardnesses (A.R.E.A. Investigation)

Date	*April 1939 (original)*	*June 1939 (after 2 mi.)*	*October 1939 (after 6 mi.)*	*May 1945* (at end of test)*
Self-quench process	315	338	355	369
Water-quench process	423	426	...	426
Air-quench process	314	329	346	361
Unhardened	246	255	273	287

* These same values were reported for measurements taken in April 1941.

Note: Procedures used in the A.R.E.A. investigation were quite different from those described in this paper as typical of current practice. They are the nearest that have been found for comparison of results over a period of years.

The author of this paper can offer no comments that would explain the apparently greater degree of work hardening, shown in Table 3, for rail ends hardened by the self-quench and air-quench processes, as compared with that for the natural unhardened rail, unless it is actually the result of the wearing away of the decarburized surface layer.

24 points Brinell, approximately the same for both methods.

Some joints in 132-lb. rail, end hardened by the compressed air quench method, were checked a little more than two months after hardening. They showed an average batter of 0.008 in. and an average hardness of 360 BHN.

Surface hardnesses were measured at various times in the A.R.E.A. investigation, no special surface preparation being made. Readings were taken about $^{3}/_{8}$ in. from the rail end and for each subsequent reading earlier locations were avoided. The reported values are given in Table 3.

The total number of rails that developed slight cracking was only 2 or 3 for each of the self-quench method, the air-quench method and the unhardened group. None of these was of sufficient size to require welding. Those in the air-quench and unhardened groups occurred at the gage corners and were head checks that had come out on the rail ends, and should not be considered as defects arising from the hardening process.

A total of 38 rails in the water-quench group developed defects. Six of these required welding.

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Welding Iron-Bearing Alpha Aluminum Bronze

Test results of improved aluminum-bronze alloy sheet with improved weldability characteristics

by F. Emery Garriott

IRON-bearing alpha aluminum bronze rolled sheet and plate conforming to A.S.T.M. B-169-48T Alloy D is a new postwar copper-base alloy developed to provide a material of construction for industrial use combining high mechanical properties, good workability, superior resistance to corrosion, erosion, cavitation and wear with excellent weldability by the various arc welding processes. This alloy also has the comparatively high thermal conductivity and the unusually high mechanical properties at subzero temperatures inherent in all copper alloys.

Although iron-free aluminum-bronze sheet and plate of the 92-8 type has been available to industry for many years, the need for an aluminum-bronze alloy having improved weldability and workability together with superior mechanical properties and corrosion resistance has long been recognized as a desirable development in the copper-alloy field.

Test results reported in this paper and specific applications to be described, indicate conclusively that this aluminum-bronze alloy can be satisfactorily fabricated by several of the welding processes, i.e., carbon-arc, metal-arc, inert-gas metal-arc, inert-gas consumable-electrode method and submerged-arc. This aluminum-bronze alloy can also be jointed to steel if required in vessel construction.

E-Cu-Al-A electrodes and covered R-Cu-Al-B filler rods are recommended for use with the metal- and carbon-arc processes, respectively. Bare filler rods (R-Cu-Al-A2) are used with the inert-gas metal-arc process and coiled wire (E-Cu-Al-A) is used with the inert-gas consumable electrode and submerged-arc processes.

Weld deposits produced in this alloy have exceptionally good hot strength and hot ductility combined with a fine grain structure. In other words, the deposits do not have a tendency to be hot short like the silicon bronzes nor do they develop the coarse, weak grain structure inherent in the tin bronzes. Arc welding of this aluminum-bronze alloy can readily be accomplished by using the proper process, electrode or filler rod, edge preparation, preheat and interpass temperature and welding technique to be described in this paper.

Table 1—Alloy Compositions (Nominal)

Alloy type	*Copper, %*	*Aluminum, %*	*Iron, %*	*Nickel, %*	*Manganese, %*
A	89.5	8.0	2.5	...	...
B	87.5	9.0	3.0	0.5	...
C	81.25	10.0	3.0	4.75	1.0

In the initial stages of the alloy development program, extending over several years three different alloy compositions were selected from a large number of laboratory alloys for additional testing on a pilot plant scale, Table 1.

Larger quantities of these selected alloys were made in the wrought form by the extrusion process for conducting the various tests necessary to determine suitability for forming, punching, shearing, corrosion resistance, welding, etc. In conjunction with this work Battelle Memorial Institute independently conducted numerous confirming tests with regards to mechanical properties of the wrought alloys and welded joints at room, subzero and elevated temperatures in a further endeavor to impartially select the alloy to be used in the final rolling-mill tests.

Alloy A, Table 1, was selected for the initial rolling and large billets were then poured under the most exacting conditions and shipped to two large rolling mills for processing into various sizes of sheet and plate. It was only after further extensive testing of the finished sheet and plate from these two rolling mills in all sheet and plate thicknesses that final approval of the alloy composition conforming to A.S.T.M. B-169-48T Alloy D was made for subsequent production in large quantities.

This particular alloy was the only one of scores made and tested in the initial and final stages of the development program meeting all the stringent requirements necessary to provide this new, "planned" construction material of copper, aluminum and iron for operation under the most severe service conditions encountered in the corrosion and subzero temperature fields.

F. Emery Garriott is Manager of the Weldrod and Wire Dept., of the Ampco Metal, Inc., Milwaukee, Wis.

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A.S.M.E. BOILER CODE APPROVAL

Since this new alloy in the wrought form was developed, primarily, for use in the construction of chemical processing equipment and other similar weldments, A.S.M.E. Boiler Code approval was secured under A.S.M.E. Special Code Case 1108 published in the A.S.M.E. Magazine of February 1950.

Briefly, the case report states that "Annealed alpha aluminum-bronze alloy of the following composition range and conforming to A.S.T.M. B-169-48T, has been approved for use in the construction of unfired pressure vessels under Paragraphs U-68 and U-69 of the A.S.M.E. Boiler Code, Special Case Number 1108":

Copper, %	88.00 to 92.50
Aluminum, %	6.00 to 8.00
Iron, %	1.50 to 3.50
Others, maximum, %	0.50

The minimum mechanical properties of the annealed, rolled sheet and plate must be:

Ultimate tensile strength, psi.	70,000
Yield strength at 0.5% elongation under load, psi.	30,000
Elongation, % in 2 in.	40.0

The maximum allowable design stresses are shown in Table 2.

Table 2—Boiler Code Allowable Design Stresses

Temperature, ° F., subzero to	*Allowable work stress, psi.*	*Allowable stress for bolts, psi.*
150	17,000 (1/4 T. S.)	6000 (1/5 Y. S.)
200	16,600 (1/4 T. S.)	5900 (1/5 Y. S.)
300	14,800 (0.8 Creep)	5800 (1/5 Y. S.)
400	11,500 (0.8 Creep)	5600 (1/5 Y. S.)
500	8,000 (0.8 Creep)	5500 (1/5 Y. S.)

With regards to weld tests, Case 1108 requires that "the outer fibers in the weld-metal bend tests shall have 20% minimum elongation. This value is also the minimum requirement for the all-weld metal tension test specimen as quoted in Paragraph U-68(e) of the Code."

ELECTRODE AND FILLER-ROD SELECTION

Although numerous welding tests were conducted simultaneously throughout the various stages during the development of this new aluminum-bronze alloy and were actually instrumental in the selection of the final composition, more comprehensive tests were, of necessity, made subsequently after study of the accumulated test data to establish the most desirable electrode-core rod and filler rod for joining this alloy.

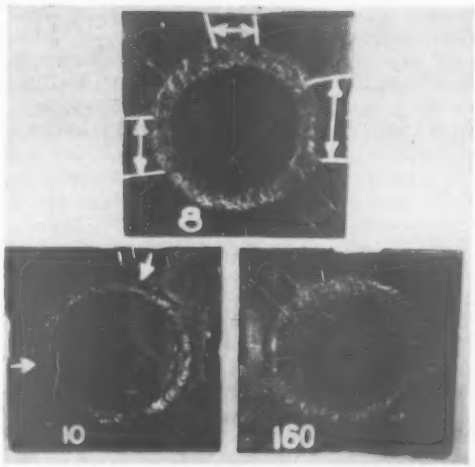

Fig. 1 Completed circle (torture) welds to test stress cracking

From past experience gained through the welding of single-phase (alpha) and duplex (alpha and beta) copper alloys of all types, it was recognized that the greatest potential difficulty presented would be possible hot shortness and the resultant cracking in the weld deposit regardless of the welding process used in fabricating this alloy.

Therefore, six different core and filler-rod compositions (Table 3) were selected for more comprehensive research work to determine the most suitable filler metal to be used in joining this alpha aluminum bronze.

Using a standard covering, these various filler metals were tested in a special, circular, restrained "torture test" (Fig. 1) to determine crack sensitivity of these filler metals used with the metal-arc and carbon-arc processes. The bare rods and wire of like compositions were tested in a similar manner using the inert-gas metal arc and inert-gas consumable electrode processes.

It is generally conceded that a circle weld is probably the most highly stressed type of joint that can be made.

Table 3—Electrode Core Rod and Filler Rod Compositions (Nominal) Tested Metal- and Carbon-Arc Processes

Alloy	*Copper, %*	*Aluminum, %*	*Iron, %*	*Nickel, %*	*Manganese, %*	*Others, %*	*Crack sensitivity*
A	91.50	8.00	...	...	...	0.50	Severe cracking welds and craters
B	89.00	8.00	2.40	0.10	...	0.50	Severe cracking welds and craters
C*	89.00	9.50	1.00	...	...	0.50	Crater cracks only
D	84.50	11.00	3.50	0.40	Tr	0.60	Crater cracks only
E	81.00	10.00	3.00	4.75	0.80	0.45	Severe cracking welds and craters
F†	84.00	11.50	3.50	0.50	...	0.50	No cracking

* A.W.S.-A.S.T.M. E-Cu-Al-A and R-Cu-Al-A2.
† A.W.S.-A.S.T.M. E-Cu-Al-B and R-Cu-Al-B.

Therefore, 8-in. circles were cut from $^3/_8$-in. alpha aluminum-bronze plate as shown in Fig. 1, beveled to a 90° over-all V groove with a $^1/_8$-in. root opening. In order to secure an even more highly stressed test assembly, the test plates were tack welded rigidly to a 2-in. thick steel backup plate so there would be a minimum of movement in any direction. This is a very drastic test and it is very doubtful if stresses of the magnitude encountered in this test plate will occur in any normal fabricated structure.

However, as the result of these tests, it was definitely established that alloy rods used having a single-phase structure prior to remelting in the arc, i.e., alloys A and B, produce deposits susceptible to severe cracking with any of the arc welding processes. In other words, the deposits exhibited low hot strength due to loss of aluminum through oxidation in the remelting process. This is characteristic of alpha-rich alloys.

Borderline-alloy core rod and filler rods such as Alloy C and D having a duplex (alpha and beta) structure prior to deposition but producing an almost wholly single-phase (alpha) structure with only a little beta present when deposited with the metal-arc and carbon-arc processes due to losses of aluminum through oxidation showed slight crater cracks in the "torture test." However, the same alloys produced crack-free deposits when applied with the shielded-arc processes since aluminum losses were much lower and the resultant deposit was, therefore, duplex (alpha and beta) in structure.

Alloy F, which is a predominantly beta bronze before and alpha and beta after deposition, was the only core and filler rod of the series tested producing consistently crack-free deposits by all the welding processes in these torture tests.

Aluminum losses through the arc when welding with the metal- and carbon-arc processes are approximately 1% for these alloys if recommended current densities are used in welding. The use of higher current densities will produce higher aluminum losses. Therefore, it is quite evident by consulting the copper-aluminum phase diagram, Fig. 2, that the behavior of these alloys under these test conditions is consistent metallurgically with the results obtained in the actual torture tests. With the 1% loss of aluminum due to burnoff, it can readily be observed that Alloys A and B would definitely be alpha rich, producing an alloy with excellent cold ductility but very poor hot ductility in the shift across the alpha solubility line. Therefore, upon cooling through the critical range, under the extremely high locked-up stress condition encountered in these tests, it would be assumed the deposit would have a tendency to crack severely.

It is more difficult to explain the behavior of Alloy E which cracked severely in these "torture tests." Although such elements as iron, nickel and mnaganese are said to have a replacement power for aluminum in aluminum bronzes, this assumption is actually incorrect in view of recent research work which indicates that

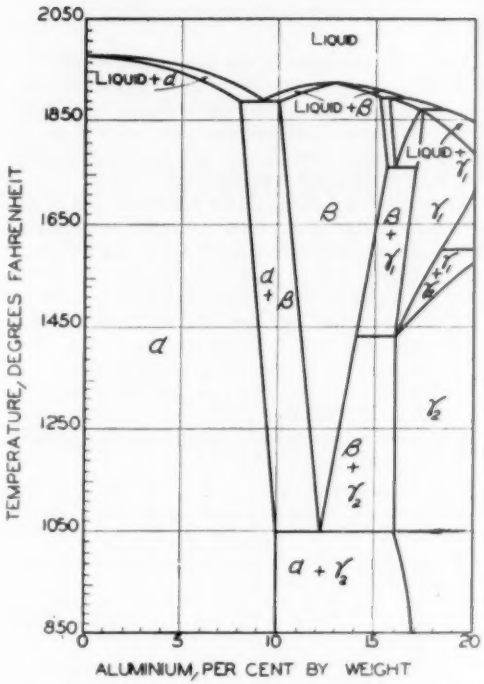

Fig. 2 Copper-aluminum equilibrium diagram

these elements combine with aluminum to form compounds, thereby robbing the alloy of aluminum causing more alpha to be rejected. These alpha-rich crystals within the alloy are comparable to a true single-phase alloy and possess hot short characteristics in cooling through the critical range evidenced in the tests.

Alloys C and D, which are duplex alloys initially, have a much lower percentage of the alpha constituents combined with some beta after remelting, thereby exhibiting only a slight tendency to crack upon cooling through the critical range which is indicated by the fact that crater cracks only occurred.

Alloy F, of course, which is a beta bronze initially, retains a very high percentage of the beta constituents after remelting with a loss of approximately 1% of aluminum through the arc. The beta bronze has higher hot ductility and lower cold ductility than alpha bronze.

This new iron-bearing alpha aluminum bronze is a heat treatable alloy. Therefore, deposit and joint mechanical properties can be improved by stress relieving at 1150° F. for one hour per inch of thickness with a fairly rapid air cool. Average deposit ductility can be increased by approximately 30% through the use of this treatment with only a slight decrease in ultimate tensile strength and yield strength.

JOINT DESIGN

Comparatively, the only differences existing between the joining of this alpha aluminum-bronze alloy and mild steel are in the basic joint design and allowances for shrinkage. This difference is due, primarily, to the fact that this alloy does have (1) a higher coefficient of thermal expansion, (2) a higher thermal conductivity, (3) a lower melting point and (4) the molten weld metal is more sluggish at arc welding temperatures.

As a result of these characteristics, which are inherent in most of the copper alloys, it is necessary to use wider grooves (Fig. 3) in butt welding and avoid sharp corners for ease of manipulation in order to minimize undercutting and the resultant slag inclusions at the side walls or interface between deposit and base metal.

When using the metal-arc and inert-gas consumable electrode processes, wider grooves of 90-degree over-all V or modified U design are required in the heavier plate thicknesses. For the processes requiring the use of filler rods (carbon-arc and inert-gas metal-arc) the edge preparation in the heavier plate thicknesses can be a 60-degree over-all V or modified U since deposition is accomplished at a slower rate of speed and the operator can more readily observe depth of penetration and wash the weld metal into the side walls more easily by manual manipulation. The more narrow groove is also recommended for use with the submerged-arc process because of the excellent, controllable penetration possible.

Also, more space should be left in square groove joints when welding lighter-gage sheet to allow for additional expansion. Tack welds should be heavier than is required in welding mild steel and at more frequent intervals of from 6 to 8 in.

A backup bar of carbon, copper or a backup strip of alpha aluminum bronze should be used in order to obtain proper root penetration. Usually, a grooved copper or carbon chill bar is preferred since the deposited metal will not adhere and it is, therefore, more easily removed after the welds have been completed.

As a general rule, higher current densities are required with all the welding processes used for a given electrode diameter in order to flow the rather sluggish weld metal effectively into the side walls in all types of joints, i.e., butt, lap, edge and tee. In other words, it is necessary to direct the flow of the molten weld metal since it will not feather smoothly due to the higher surface tension of the molten alloy which is typical of all aluminum bronzes when welding like base metals.

The usual precautions with regards to cleanliness of the edges to be joined should be practiced in welding alpha aluminum bronze. Grease, dirt and moisture will cause excessive porosity in the deposited metal.

CARBON-ARC WELDING PROCESS

The carbon-arc process is, generally, the preferred method recommended for joining this iron-bearing alpha aluminum bronze since more extensive basic fabrication data and experience does exist with reference to this process at the present time, especially, for joining heavy sections. Maximum deposit density can be obtained through the use of this process in welding the heavier sections although distortion is of greater magnitude since more heat is developed in the joint.

Covered filler rods of the A.W.S.-A.S.T.M. classification R-Cu-Al-B must be used in welding this alloy since the very refractory aluminum oxides formed during the remelting operation must be fluxed effectively in order to secure a weld deposit of high quality.

Straight (negative) polarity, direct current must be used. It is not necessary to preheat unless the plate is exceptionally cold and for plate thicknesses of 1 in. or greater. All welding should be done in the downward position.

Tapered carbon electrodes of the high graphite type have been found to be most satisfactory for welding this alloy. The taper should be formed by filing or grinding the carbon to a conical taper 4 to 5 in. in length. The size of the electrode to be selected depends upon the filler rod diameter and current required for the specific application. Generally, the current ranges shown in Table 4 have been found desirable for use with carbon-arc welding.

The arc should always be carried on the weld deposit. The molten metal from the end of the filler rod should be deposited in the weld pool and flowed into the side walls to avoid slag entrapment and secure a sound bond.

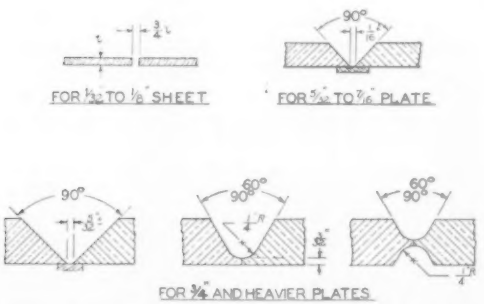

Fig. 3 Recommended groove designs for alpha aluminum bronze

Table 4—Current and Voltage Data

Electrode diam., in.	*Filler rod diam., in.*	*Amperage*	*Voltage*
	Carbon-arc process		
3/16	1/8, 5/32	60–80	...
1/4	5/32, 3/16	110–130	...
5/16	3/16, 1/4	190–250	...
1/2	1/4, 5/16	280–350	...
	Metal-arc process		
3/32	...	80–100	26–28
1/8	...	140–150	27–30
5/32	...	150–170	29–32
3/16	...	180–230	30–34
1/4	...	290–330	32–36
5/16	...	340–375	32–36

Table 5—Average Deposit Mechanical Properties Iron-Bearing Alpha Aluminum Bronze

Process	*Electrode or filler rod*	*Pre-heat, ° F.*	*Inter-pass, ° F.*	*Ultimate tensile strength, psi.*	*Yield strength, 0.5% Elong., psi.*	*Elonga-tion, %*	*Reduc-tion of Area, %*	*% Elong.-Bend*	*Hard-ness, Rb*
Carbon-arc	R-Cu-Al-B	...	300	87,900	36,500	23.4	23.5	25.0	94
Metal-arc	E-Cu-Al-A	350	350	76,800	37,900	32.1	29.4	31.3	85
Inert-gas metal-arc	R-Cu-Al-A2	..	300	78,600	37,600	25.3	27.6	30.2	86
Inert-gas consum-able electrode	E-Cu-Al-A	350	350	80,000	36,000	26.5	31.5	47.9	84
Submerged-arc	E-Cu-Al-A	..	..	79,700	37,500	22.0	21.0	24.6	90
Plate	A.S.T.M.B-169-48T Alloy D*	..	..	70,000–75,000	30,000–42,000	30.0–40.0	35.0–40.0	..	80–82

* Mechanical properties vary with plate thickness. High values, light gage sheet. Lower values, heavier plate.

The graphite electrode should be held 15 degrees to the horizontal in the direction of travel. The arc length should be approximately equivalent to the filler rod diameter. With the wider grooves used, the electrode should be manipulated with a weave motion directing the weld metal into the side walls. Excessive puddling of the weld metal must be avoided since overheating will result in high aluminum losses resulting in an alpha-rich, hot short deposit.

By following these few simple rules, sound, homogeneous welds can be secured in iron-bearing alpha aluminum bronze producing mechanical properties shown in Table 5. Stress relieving at 1150° F. will improve the ductility of the weld metal and the joint.

INERT-GAS METAL-ARC PROCESS

Dense, high-quality welds of excellent appearance can be consistently obtained through the use of the inert-gas metal-arc process for welding this iron-bearing alpha aluminum bronze. Usually, edge joints in light-gage sheet can be used requiring no filler-metal addition. In welding the heavier sections, a bare filler rod of the A.W.S.-A.S.T.M. classification R-Cu-Al-A2 should be used to produce high strength, ductile welds. Unlike the carbon-arc process which requires the use of covered filler rods of higher aluminum content, bare rods of lower aluminum content are used with this process because of the excellent shielding secured. Therefore, aluminum losses through oxidation are very low if the proper flow of gas is maintained.

Superior welds are obtained by using alternating current although straight polarity, direct current can also be used for many applications. Either standard transformers with continuous high-frequency stabilization or high open-circuit-type transformers designed for this process can be used satisfactorily.

Argon or helium gas may be used in joining alpha aluminum bronze although the best head appearance is secured through the use of argon. There is, also, less spatter loss with argon. Greater penetration is secured by using helium. Therefore, argon is used for welding light-gage alpha aluminum bronze and helium is more desirable for use in welding the heavier-plate sections. Experiments are being conducted with mixtures of these two gases and the preliminary tests seem to produce very satisfactory results. The softer arc action of argon and the deeper penetration secured through the use of helium seems to be the result of combining these gases in various percentages.

It has been found that thoriated tungsten electrodes produce deposits with a minimum amount of tungsten inclusions. The use of regular tungsten electrodes will produce deposits with excessive quantities of included tungsten. Also, care must be taken to avoid condensation of moisture in the torch since moisture will cause porosity and the deposition of tungsten in the finished weld. Often-times, in practice, tungsten deposits are obtained because operators do not want to change to a larger electrode size when current densities are increased with changes in sections in a weldment. Another advantage to the use of thoriated tungsten is that a larger electrode can be used under such circumstances and the current density reduced as lighter sections are approached. Overheating of the tungsten, even when using the thoriated type, will cause excessive amounts of tungsten to be deposited in the weld metal.

For joining alpha aluminum bronze, preheating is not necessary when using this process except for welding heavy sections of 1 in. or greater. All welding should be done in the downhand position and no special welding technique is required to secure average mechanical properties shown in Table 5.

METAL-ARC WELDING PROCESS

The metal-arc welding process can be used for fabricating this iron-bearing alpha aluminum bronze where increased speed of welding is preferred to maximum weld quality. However, it is more difficult to secure perfectly sound deposits consistently with this process by all welding operators and is not recommended if slight porosity is unacceptable for the application. Distortion can be controlled more readily through the use of metal-arc welding since less heat is developed in the joint than through the use of processes requiring filler rods.

Recent experiments have indicated that electrodes of the A.W.S.-A.S.T.M. E-Cu-Al-A classification should be used in joining this new alpha aluminum bronze be-

cause weld metal ductility is higher than could be obtained through the use of E-Cu-Al-B electrodes. These electrodes have a light flux covering specially designed to remove the very refractory aluminum oxides formed in the remelting process. Heavier coverings have been tried unsuccessfully since they seem to cause extremely deep penetration, high spatter loss and severe undercutting resulting in a more porous deposit.

A considerable amount of research and development work has been done to improve the covering of these electrodes in order to produce welds at high deposition rates of consistently high quality with operators that are not too highly skilled. In fact, recent tests have indicated that the development of such an electrode is feasible. However, laboratory tests must be confirmed by actual production experience over a long period of time before a new product can be released and recommended for general usage.

These electrodes are used in the downhand position only with reverse (positive) polarity, direct current. Preheating to 300–400° F. is essential on heavy sections and this same interpass temperature must be maintained if satisfactory welds are to be secured. Generally, higher current densities are required for welding alpha aluminum bronze as compared to steel in conjunction with this preheat and interpass temperature in order to flow the weld metal properly. See Table 4 for recommended current values.

Either string or weave beads may be used with equal success although weave beads are usually preferred since there is less chance of obtaining slag inclusions. In general, the largest diameter size of electrode consistent with good practice for the plate thickness should be used. The electrode should be manipulated with a rapid weaving motion to deposit a bead having a width approximately 3 to 5 times the diameter of the electrode hesitating at the edges in order to fill undercutting and avoid slag entrapments. Electrodes should be inclined slightly in the direction of welding so that the slag will flow back over the weld pool. All slag must be thoroughly cleaned between beads.

Weld deposits of high quality can be secured with the metal-arc process producing deposit mechanical properties shown in Table 5 by using the above procedure. Short-cut methods cannot be used in the metal-arc welding of iron-bearing alpha aluminum bronze or slag inclusions; porosity and poor mechanical properties of the deposited metal result.

INERT-GAS CONSUMABLE ELECTRODE METHOD

Extensive substantiating research and development work has been conducted by several groups independently confirming the fact that the inert-gas consumable electrode method can successfully be used to join this new alpha aluminum bronze producing sound deposits with good mechanical properties. In fact, this is the only process that has been found to satisfactorily weld aluminum bronzes in the vertical and overhead positions using 0.035- and 0.045-in. diameter wire of similar composition.

Additional advantages to be gained in the welding of alpha aluminum bronze by this process are those found for depositing other metals and alloys which are, primarily, higher rates of deposition, dense deposits, good mechanical properties (Table 5), less distortion and excellent deposit appearance.

With $^1/_{16}$-in. diameter wire, feeds of 100, 200 and 300 in. per minute have been used at welding currents of 135, 240 and 325 amp. respectively to obtain deposition rates of approximately 5, 10 and 15 lb. per hour.

Either argon or helium gas may be used with reverse (positive) polarity, direct current. For flat position welding, $^1/_{16}$- and $^3/_{32}$-in. diameter size bare coiled wire of the A.W.S.-A.S.T.M. classification E-Cu-Al-A is recommended for use. The arc action with argon gas is smooth, stable and spatterless producing a bright appearing bead with lower penetration than is obtained through the use of helium. Although the surface appearance of the beads is better when using argon gas, the deposits are more porous than those obtained with helium. The deeper penetration secured through the use of helium is desirable for welding the heavier plate thicknesses. A dark oxide film covers the bead made with helium producing a less attractive appearing bead than is secured with argon. However, mechanical properties of deposits made with helium are superior since the deposits are more sound.

Rate of gas flow, welding speed and direction of travel seem to affect the mechanical properties of the weld deposit. For example, when using $^1/_{16}$-in. diameter wire at a wire feed of 220 ft. per minute in $^3/_8$-in. aluminum bronze plate, it has been found that deposit mechanical properties as shown in Table 5 can be obtained with helium using a gas flow of 100 cfh. at 260 amp. using a following arc (backhand). If travel is in the opposite direction (leading the arc) or, maintaining all other conditions, if gas flow is changed to 50 cfh. ultimate tensile strength may be approximately 10,000 psi. lower with a loss in ductility of approximately 5 percentage points. If the proper gas coverage is not maintained by traveling at too fast a rate of speed or by the use of insufficient gas, fine transverse cracking may occur in the deposit as the result of oxide inclusions at the grain boundaries.

Recent tests indicate that sound welds of good appearance can be secured through the use of a mixture of gases in a ratio of 80% helium to 20% argon. Spatter loss is reduced and penetration is excellent.

A preheat and interpass temperature of 300–400° F. is essential in order to secure the best results. Arc length within normal limits does not seem to materially affect the resultant mechanical properties although a short arc is preferred for the initial layer in the groove because better penetration is secured than with a longer arc length.

SUBMERGED-ARC WELDING

Although the submerged-arc process has not been used to produce weldments of this new alpha aluminum bronze because this process is applied generally to the fabrication of assemblies made in quantity, nevertheless sufficient research work has been conducted to determine that the process can be used successfully.

In the initial stages of this work, such standard melts as Linde No. 50, 12 x 150 and H, 12 x D were tried in groove welding. It was found these melts were unsuitable due to alloy pickup from the melt which increased ultimate tensile strength and reduced the ductility of the deposited metal. However, a new melt (Linde AB) was developed for use with E-Cu-Al-A bare coiled wire which produces deposits with mechanical properties similar to the average shown in Table 5. The new melt contains low-melting-point fluorides similar to those used in the coverings of the metal-arc welding electrodes providing excellent protection from oxidation.

E-Cu-Al-A bare wire, with reverse (positive) polarity, direct current, applies in a manner similar to mild steel except the weld metal is more fluid at welding temperatures requiring a faster rate of travel or conversely lower current densities per wire diameter size. Preheating is not required except in very heavy sections. A backup of carbon, copper or alpha aluminum bronze of like composition is required in order to avoid excessive burnthrough.

COMMERCIAL APPLICATION

In a relatively short time, the unusual combination of desirable metallurgical and mechanical characteristics inherent in this new iron-bearing alpha aluminum bronze has been recognized by the chemical processing industry as suitable for use in combating severe corrosive action and an increasing amount of the new alloy is being used in the fabrication of production installations.

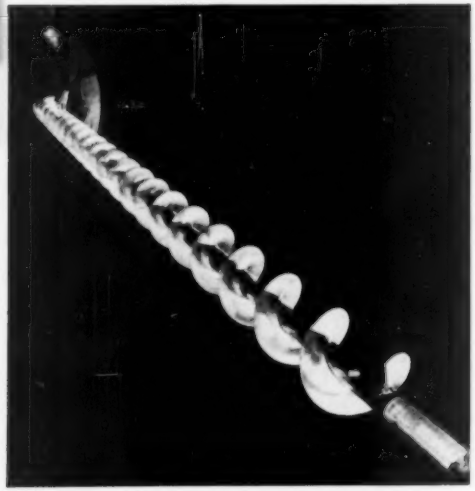

Fig. 4 Aluminum bronze screw conveyor, formed and carbon-arc welded

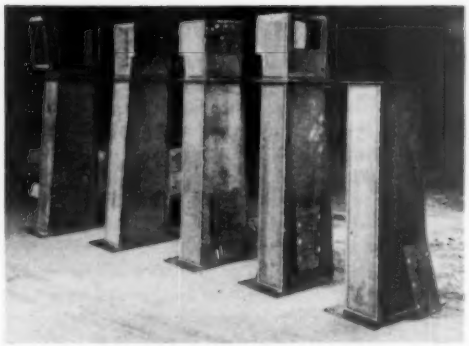

Fig. 5 Fabricated chutes to resist erosion and corrosion

A few representative weldments will be described in order to illustrate the versatility of this alloy as a material of construction. Detailed information cannot be given in every case history because of competitive and Government security reasons.

Probably the simplest example of construction of a fabricated assembly made from alpha aluminum bronze is the large, heavy-duty screw conveyor shown in Fig. 4. It was fabricated entirely from this alloy using the carbon-arc process. The spiral is 19 $^1/_2$ ft. long and 12 in. in diameter. The flights were made from $^1/_4$-in. plate formed to shape.

The conveyor is simple in construction, yet rigid (no center bearing in the 19-ft. 6-in. length). The flights were pressed into shape and then welded to the center shaft with R-Cu-Al-B filler rods using the carbon-arc process. Each individual flight section was then butt welded to the next section until the entire length was built up as required. The high strength of the weld eliminates the necessity of a continuous bead between the shaft and the flight.

This particular screw conveyor is being used in a large eastern chemical plant for a process involving sulphuric acid as the corrosive media. The ability of this alloy to resist abrasion, cavitation, corrosion and erosion makes it an ideal material for construction of

Fig. 6 Conveyor housing used in manufacture of synthetic alcohol

Fig. 7 Centrifugal separator used to withstand severe corrosive action

such jobs as occur in many solids conveying applications.

The five fabricated chutes shown in Fig. 5 were fabricated for use by one of the largest potash-producing companies in the country. Each one is approximately 7 ft. high and has an average cross-section area of 4 sq. ft. Material used throughout was $^1/_4$-in. iron-bearing aluminum-bronze plate with the exception of the lid handles and cover plates. All mating surfaces and gasket faces were prepared for the use of $^1/_{16}$-in. thick gaskets.

Hard, unannealed plate had to be used in this particular application to resist the very severe erosive effects of the brine mixture. The plate had a minimum Brinell hardness of 180 (3000 kg load).

Despite the high hardness of the parent metal, all welds (both edge and fillets) made with the carbon-arc process using R-Cu-Al-B filler rods were free from any cracking whatsoever. No edge preparation was necessary. Also, no preheating or postheating was used since such a practice would have softened the plate adjacent to the weld and thus nullified the erosion-resisting qualities of the plate.

The conveyor housing shown in Fig. 6 was fabricated from $^3/_{16}$ in. iron-bearing aluminum bronze with the carbon-arc process for a manufacturer of synthetic alcohol. The alcohol is made from corn cobs and other waste farm products that pass through a hydrolizer containing

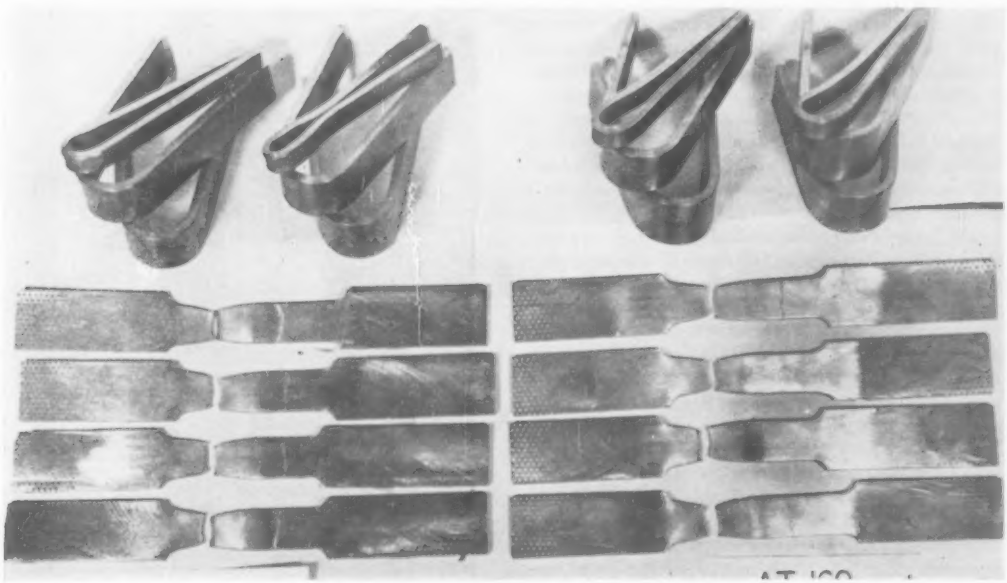

Fig. 8 Mild steel to aluminum bronze, all joint specimens broke in steel

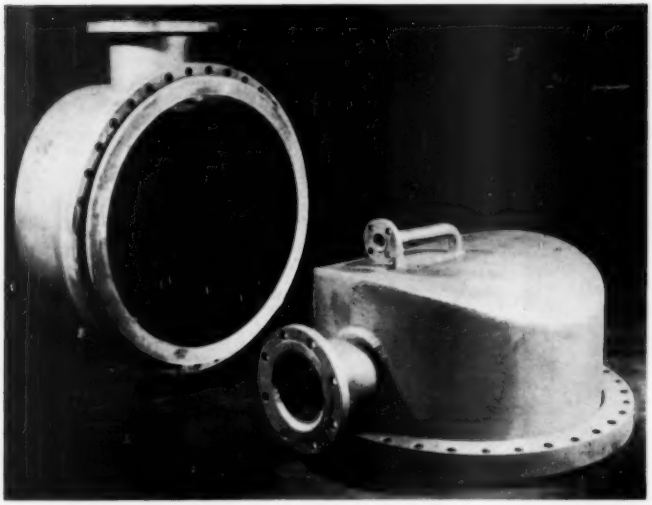

Fig. 9 Formaldehyde thermosyphon reboiler heads

sulphuric acid solution. Upon removal from the hydrolizer, the pulp is thoroughly soaked with acid.

The conveyer chain used in the removal process was also cast from a comparable alloy as were all of the angles used which were extruded.

All fabrication was done in the downhand position with R-Cu-Al-B filler rods and the carbon-arc process on the specific assembly shown. Later the inert-gas metal-arc process was used for fabricating these conveyor housings using bare rods in order to secure edge welds of improved appearance. Obviously, either deposit has the same corrosion-resistant characteristics as the parent metal. Again, this alloy was selected because of the exceptional resistance to corrosion, erosion, abrasion and cavitation which is encountered in the routing of liquids, semifluids and liquids containing solids in suspension such as: acids, petroleum sludge, alkaline solutions, sea water, mine water, food product liquors, etc.

The centrifugal separator shown in Fig. 7 was constructed from iron-bearing aluminum bronze and flange quality steel. The weldment was constructed from $^{3}/_{16}$- and $^{3}/_{4}$-in. plate to withstand a hydrostatic test pressure of 100 lb. All welding was done in the downhand position using the carbon-arc process with $^{5}/_{32}$-in. diameter covered filler rods of the R-Cu-Al-B classification. All butt joints were made using a 90-degree single-V groove. Preheating was not required when using the carbon-arc process because of the heat developed in the joint through the use of this process.

Van Stone type flanges were employed throughout to take advantage of the lower cost of steel flanges which do not come in contact with the corrosive media. The Van Stone rings were cut from iron-bearing aluminum-bronze plate and then slipped over the pipe nozzle. The rings were welded to the pipe on the inside and outside diameters after which the gasket surface was finished by hand grinding.

The large cover plate was made by welding a thin aluminum-bronze sheet directly to a $^{3}/_{4}$-in. thick steel backup. This weld was also made with $^{5}/_{32}$ in. diameter R-Cu-Al-B filler rod and the carbon-arc process. The supporting brackets were made of steel and welded directly to the aluminum-bronze shell by means of fillet welds.

Flat, joint tensile specimens, shown in Fig. 8 were made by joining mild steel to iron-bearing aluminum-bronze plate break, when pulled, in the mild-steel parent metal. Guided bend tests from the same test plate bend 180 degrees without failure, proving that satisfactory dissimilar metal welds can be produced in this manner.

The formaldehyde thermo-syphon reboiler heads shown in Fig. 9 were fabricated from $^{1}/_{2}$-in. iron-bearing aluminum-bronze plate with the carbon-arc process

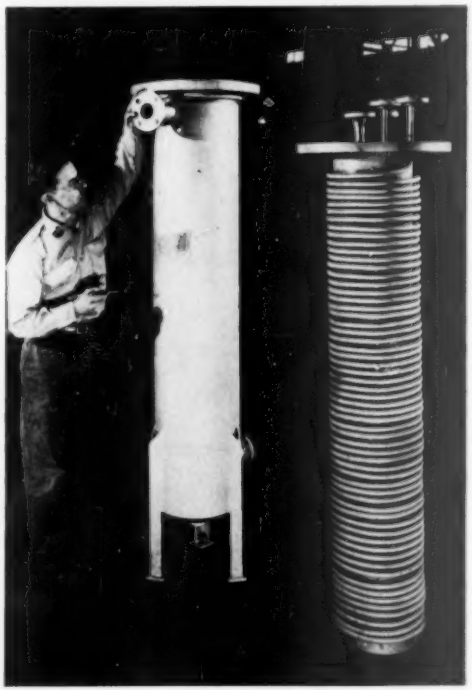

Fig. 10 Continuous tube heat exchanger fabricated from alpha aluminum bronze

Fig. 11 Tray assembly in aluminum bronze fractionating tower

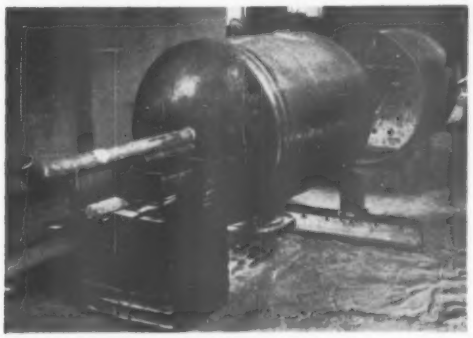

Fig. 12 Two end sections of fractionating tower showing circumferential seam and nozzle positions

using $^{3}/_{16}$ in. diameter covered R-Cu-Al-B filler rods. The entire assembly was fabricated from this alpha aluminum bronze with the exception of the loose steel flanges used for the Van Stone joints. These flanges were made of A-285 Grade C flange quality steel.

The design pressure was 100 psi. and the design temperature was 250° F. The assembly had to pass a 150-lb hydrostatic test upon completion.

The cylindrical portion was rolled from $^{1}/_{2}$-in. plate and butt welded. The head was rolled to a 30 in. radius and joined to the cylindrical part with an edge weld. The nozzle was $^{3}/_{8}$-in. stock with a 1 $^{3}/_{16}$-in. flange and attached to the shell section with fillet welds.

These heads are used in conjunction with a column manufacturing formaldehyde. Tube sheets 3 in. thick by 36 in. in diameter were also produced for the over-all assembly from alpha aluminum bronze. .

The continuous tube heat exchanger shown in Fig. 10 was produced from iron-bearing alpha aluminum bronze. The inside shell was made from 10-in. Schedule 40 welded aluminum-bronze pipe and the outside shell was produced from 14-in. Schedule 40 welded aluminum-bronze pipe. The legs were made of mild steel and attached to the shell using the carbon-arc process with $^{5}/_{32}$-in. diameter R-Cu-Al-B filler rods.

The sections of tubing were joined by means of the inert-gas metal-arc process using bare $^{1}/_{8}$-in. diameter R-Cu-Al-A2 filler rods. The tubing is $^{3}/_{4}$-in. diameter 16 gage and it required 250 ft. to complete the assembly.

This continuous tube heat exchanger was completed by welding in the flat heads using a modified J groove for a full penetration weld. The nozzles in the upper flanged head were attached by means of fillet welds on both sides of the head.

Sections of a fabricated fractionation tower are shown in Figs. 11 and 12. This 10,000 lb. fabricated fractionation tower was made entirely under the A.S.-M.E. Boiler Code in agreement with special Case No. 1108. Welder qualifications were conducted and approved by the Ocean Accident and Guarantee Co. using both the carbon-arc and inert-gas metal-arc process with covered R-Cu-Al-B and bare R-Cu-Al-A2 filler rods respectively.

This tower is in service in a large southern chemical plant producing acetic acid from butane gas by petrochemical processes. The alloy was selected because of its excellent resistance to this corrosive medium, high strength and erosion-resistance characteristics.

The tower shell, as well as internal parts such as trays, downcomers and dams, was constructed of $^{3}/_{8}$-in. thick iron-bearing alpha aluminum bronze. The vapor risers are 2 $^{1}/_{2}$-in. O.D. by 2-in I.D. extruded tubing of the same alloy. Van Stone type nozzles were utilized throughout with steel backup flanges.

The skirt of the tower was made of $^{1}/_{2}$-in. thick carbon steel which was welded directly to the bottom head by the carbon-arc process without preheating or postheating.

Figure 11 illustrates the manner in which the trays were welded into the assembly by means of fillet welds using $^{3}/_{16}$-in. diameter filler rods. Figure 12 shows the two end sections upon completion and prior to assembly of the two center sections and illustrates the manner in which the dished heads were joined to the shell.

Figure 13 shows part of an installation recently produced for a large midwestern chemical company. Twin units for the production of esters from alcohols and acids included steam-jacketed kettles, packed towers, overhead condensers, separators, valves and piping. The completed units were made from iron-bearing alpha aluminum bronze.

The steam-jacketed kettles were designed for 75 psi. external pressure and built out of $^{1}/_{2}$-in thick aluminum bronze, while the jackets were made out of $^{1}/_{2}$-in. thick S.A.E. 1020 steel. The A.S.T.M. standard flanged

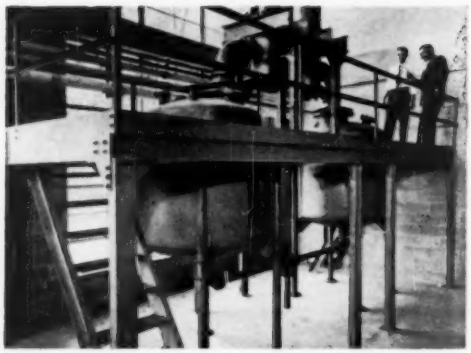

Fig. 13 Aluminum bronze steam-jacketed kettles for production of esters from alcohol

dished heads were also produced from 1/2-in. thick aluminum-bronze plate.

The towers, condensers and separators were produced from 1/4-in. thick aluminum-bronze plate while the tubing was 5/8-in. O.D., 18-gage aluminum bronze conforming to A.S.T.M. B-11 specification. Split-disk gate valves were made from castings of aluminum bronze having a higher aluminum content.

All welding was done in the downhand position with covered R-Cu-Al-B filler rods using the carbon-arc process for the initial layers and the inert-gas metal-arc process with bare R-Cu-Al-A2 for some of the finish beads for better appearance. In the butt welds, one string bead was applied from the outside using a 90-degree over-all V with a 1/8-in. lip. Following the string bead, all seams were back chipped from the inside and finished before the outer welds were completed.

When the aluminum-bronze kettle was completed and before attaching the steam jacket, the assembly was stress relieved at 650° F. and air cooled. Stress relieving was employed because of the proximity of the 18-in. outlet nozzle to the head seam and because of the large number of nozzles in the head. None of the other equipment was stress relieved after welding.

These steam-jacketed esterification stills and columns were fabricated in accordance with the A.S.M.E. Boiler Code for Unfired Pressure Vessels under Paragraph U-68 and U-69.

CONCLUSION

It is evident from the foregoing test data and application information that iron-bearing alpha aluminum-bronze sheet and plate can satisfactorily be fabricated by the various arc-welding processes, i.e., carbon-arc, metal-arc, inert-gas metal-arc, inert-gas consumable electrode and submerged-arc. The alloy cannot, however, be welded with the oxyacetylene process since a satisfactory flux has not been developed to adequately remove the very refractory oxides formed in welding.

The mechanical properties of the weld metal produced by any of these arc-welding processes are comparable to those of the base metal with the exception of ductility which is lower by the percentage difference existing in steel welding.

The weld deposits produced in iron-bearing alpha aluminum bronze using the electrodes and filler rods recommended have the same corrosion-, erosion-, cavitation- and wear-resistant qualities found in the base metal and inherent in this type of alloy.

Needless to say, the information contained in this paper regarding the arc-welding of this new copper-base alloy is far from complete because of the required brevity of the article. However, it may be indicative of the amount of research and development work completed in the past and does provide some fundamental principles to be considered in the fabrication of this alloy by arc welding.

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Product Design for Welding

Equipment and method to economically weld automatically production jobs of moderate quantities

by John Mikulak

INTRODUCTION

THE supply of skilled man power has become a very critical item confronting the industries of our country. To alleviate this condition, it has become necessary for the production engineer and product designer to devise production methods, which, with suitable designs, will transplant the required skill from man to machine. Only by success of this evolution can the constantly increasing demands of production volume and performance of intricate mechanisms be satisfied.

In the past there has been a tendency to reserve so-called semi- or fully automatic high-production machines for large-volume production. This was true of machine tools as well as welding equipment. In the welding industry it is still quite commonly expressed that a particular job would be an ideal application for automatic welding if there was considerably more production of the particular item or if the item were of considerably larger size and of heavy-plate construction.

It is necessary to disregard the latter logic and substitute one which will provide universal tooling and methods for the automatic process to quickly weld joints of relatively short lengths and of infrequent identical occurrence. This mode of thinking demands greater ingenuity on the part of the product designer and the tool engineer. The welding engineer has available versatile automatic and semiautomatic welding equipment and can provide substantial assistance in this matter. There are new processes and equipment that require less exacting tolerances on joint fit, plate preparation and base-metal metallurgy which will facilitate application of them to general product design. Low-hydrogen electrode and fluxes, twin-arc submerged-welding machines and automatic gaseous shield-arc equipment can be named to illustrate a few of the later developments now being applied commercially.

Inasmuch as equipment and methods must be produced to fit the design, the design is considered basic and consequently in the following a discussion of design is given first, after which a description is given of equipment and methods available to economically weld these designs

DESIGN—GENERAL

A wide selection of design approaches is available and is made necessary when providing performance features which are required of products manufactured for various applications. Care must be taken that all codes are satisfied and, also, that the design is treated to incorporate the trade advantages generally supplied by the product. The design must not require fabrication operations which cannot be economically produced by existing equipment in the shop or, where possible to cost advantage, from outside suppliers. Due to the above limitations, the economics involved are, to a great extent, an individual problem. However, there are several fundamentals that must be adhered to, to obtain best economy and these are listed as follows:

1. Use materials possessing the best possible weldability. When using alloys they should be chosen to require minimum pre- and post-welding operations. Further, the material should possess weld environment which will assure suitable metallurgical and physical joint properties.

2. Use as few elements as possible to compose a structure and determine that each is performing a functional service. Investigate the use of pressings, castings and forgings to produce a composite design with improved cost and product-performance results.

3. Decrease the amount of welding to a minimum by making use of bending and also by specifying joints which require a minimum amount of deposited weld metal.

4. All welding should be clearly shown and specified on drawings and weld sizes held to a minimum. All drawings should show tolerances on fabrication and assembly operations and these tolerances should be specified to produce optimum conditions at root openings of joints to be welded as well as allowing for distortion encountered when welding.

5. Specify joints where good fit-up is obtained with minimum effort in the fabrication and the assembly operations.

6. Use joints which can readily be welded with the semi- or automatic weld processes. Where manual arc welding is used, make certain that the joint fit-up allows use of the largest electrode possible. On heavy open-hearth steel structures under-bead cracking can be taken care of by a suitable electrode such as the low-hydrogen type.

7. Joints should be specified so that welding is balanced as nearly as possible around the neutral axis of the structure to assist controlling distortion. Joints specified should be placed so that minimum grinding and cleaning is required.

8. To decrease handling the design should be such that all welding, if possible, can be applied from one side of the structure. In some instances this may require building the product from one or more subassemblies which later can be placed and welded in a final assembly jig. The latter procedure can also be used to produce large production output with maximum shop efficiency and lower jig cost.

9. Proper assembly and weld procedures should be provided to standardize product quality and cost. These procedures should utilize maximum benefits obtainable from the welding jigs and equipment provided for production.

10. Welding-shop floor should be so organized, especially on heavy manual welding and automatic welding, that maximum operator factor is maintained by the welding operators. This requires that setup, poor-fit repair and jig-disassembly operations, where possible, should be done by others rather than the weld operator.

11. Universally applied automatic weld equipment and universal jigs should be provided to produce maximum arcing time. Standard automatic welding heads can be modified to provide ample tolerances on joint openings to allow use of conventional fabrication methods produced by

John Mikulak is connected with the Worthington Pump and Machinery Corp.

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properly operating regular steel shop fabricating equipment.

12. Fabricating operations should be produced by using gages and tools ordinarily supplied with fabricating equipment. Shearing and bending plates to chalk marks, either scaled off on the piece or traced from a template, can quite readily provide high production cost, especially in the set-up and welding operations.

DESIGN APPLICATION

In the following it is assumed that the general fabrication shop is considered rather than a mass-production type of shop. Therefore, it is necessary to consider relatively small quantities where setup operations of jigs are of frequent occurrence. Consequently, the initial task in design is to set up an arbitrary set of design standards and later have the designs correlated to correspond. This approach will provide interchangeable parts or subassemblies on the various sizes of units or products being manufactured to produce larger volume of identical or similar operations. As a result improved economy will be obtained in material purchasing, material and parts storage, material and parts handling, inventory turnover as well as production operation. Further, this approach will simplify construction of universal jigs that will accommodate a partial or complete group of various size units making up an equipment line.

By making a survey of all the parts being produced it is possible to establish a general design approach to cover fabrication and assembly needs. In making this survey, machine and fabrication equipment not available may more readily be proved desirable and, if purchased, will provide maximum assurance of anticipated payback and product-cost results as well as product performance. In this survey consideration should be given to produce component parts in a group of general-purpose jigs and, further, determine where pressing, bending, shearing or torch cutting will be used for fabricating component parts. This survey should also determine where and when parts made by casting and forging will be introduced into the design as elements of the weldment. Further, this survey should provide standardization of material analysis to be used, and plate, bar and shape sizes to minimize the problems and hazards encountered when myriads of materials and sizes are collected in stores. Under present market conditions, the latter may prove difficult to execute without substitutions. However, the program will be in process to make use of economies when and where they can be employed.

This type of data can be worked up in chart or graph form suitable for quick reference. The design department should have for quick reference, over-all dimensions and tonnage capacity of equipment such as press brakes, power bend brakes, shears, planers, milling and boring equipment. The design department should also have available drawings of existing press and bending tools with notation of permissible variations such as flange heights and material stock thickness. The maintenance and physical condition of dies and tools is a production-department activity and need not concern the design department except that the latter should be notified when a die or tool is to be reworked or replaced. At this time the design department should have an opportunity to request desirable changes in the particular tool or die.

Other data that should be available in the design department are charts showing where break-off points occur between bending sheets of various thicknesses for various lengths and various quantities against shearing or torch cutting separate plates and welding them together. A chart of this type is illustrated in Fig. 1 where pressing costs are on the left-hand side, and manual and automatic welding costs are on the right-hand side. In both instances cost covers fabrication, setup and handling of material for making the parts suitable for the final operation of either bending or welding and, in the case of welding, the cleaning operation to remove flux and splatter. No material cost is included in this chart as it is assumed identical in both cases. Figure 1 illustrates the definite effect of automatic welding on the cost economy in a weldment. It is to be noted that this figure is for a small number of pieces. As the number of identical pieces increases, the economy rate favors bending. In the bending cost, time is allowed for adjusting dies, ram travel and stop gages but not for changing dies. The latter is done deliberately because it will be found that a standard die set will perform satisfactorily for a large number of different jobs in right angle-bending assumed in this figure.

Another chart for quick reference is shown in Fig. 2 and indicates the approximate cost per pound of simple low-carbon steel castings against properly designed weldments of the same weight produced

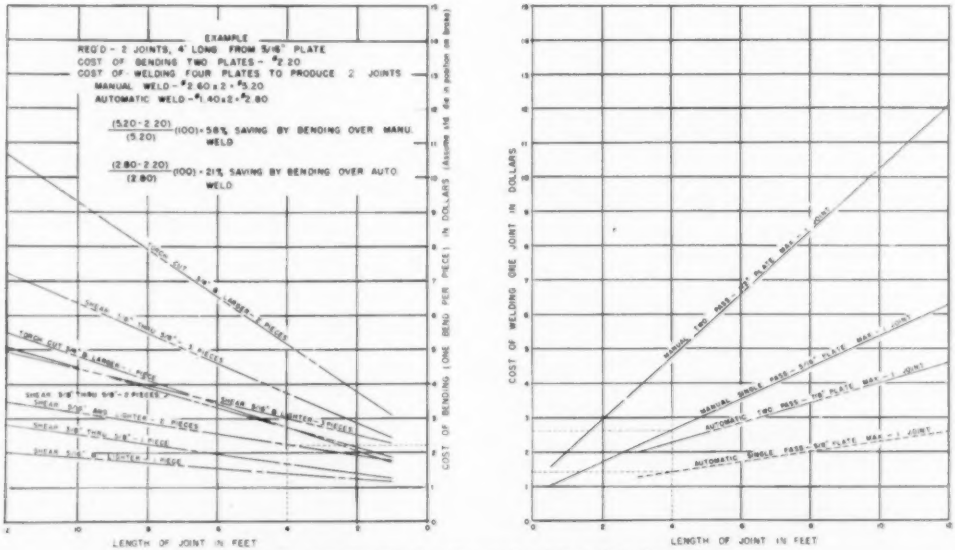
EXAMPLE
REQ'D - 2 JOINTS, 4' LONG FROM 5/16" PLATE
COST OF BENDING TWO PLATES - $2.20
COST OF WELDING FOUR PLATES TO PRODUCE 2 JOINTS
MANUAL WELD - $2.60 ± 2 = $5.20
AUTOMATIC WELD - $1.40 ± 2 = $2.80
(5.20 - 2.20) / (5.20) (100) = 58% SAVING BY BENDING OVER MANU. WELD
(2.80 - 2.20) / (2.80) (100) = 21% SAVING BY BENDING OVER AUTO. WELD
TORCH CUT 5/8" & LARGER - 1 PIECE
SHEAR 1/2" THRU 5/8" - 2 PIECES
SHEAR 5/8" & LIGHTER - 2 PIECES
SHEAR 5/8" THRU 5/8" - 1 PIECE
SHEAR 5/8" & LIGHTER - 1 PIECE
COST OF BENDING (ONE BEND PER PIECE) IN DOLLARS (Assume 1/4" die in portion on brake)
LENGTH OF JOINT IN FEET
COST OF WELDING ONE JOINT IN DOLLARS
MANUAL TWO PASS - 5/8" PLATE MAX. - 1 JOINT
MANUAL SINGLE PASS - 5/16" PLATE MAX. - 1 JOINT
AUTOMATIC TWO PASS - 5/8" PLATE MAX. - 1 JOINT
AUTOMATIC SINGLE PASS - 5/16" PLATE MAX. - 1 JOINT
LENGTH OF JOINT IN FEET

Fig. 1 Cost per foot for bending and welding of 90-degree joints

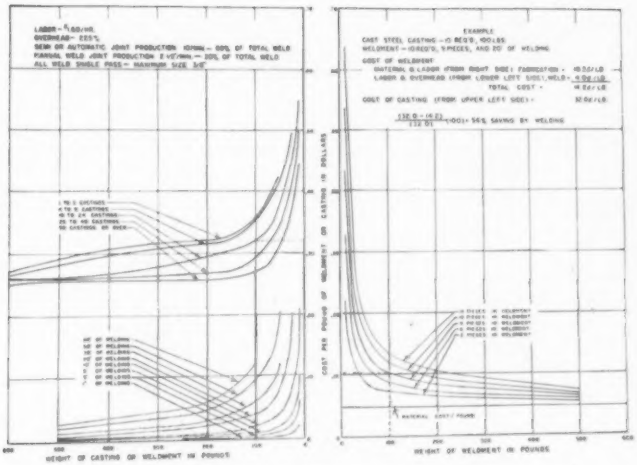

Fig. 2 Cost per pound of castings and weldments in steel

from open-hearth steel. The casting costs in the upper left-hand side allow for weight classification and casting quantities but do not allow for original pattern cost. To obtain the cost of the weldment it is necessary to add the material and fabrication cost on the right-hand side to the welding cost on the lower left-hand side. In the latter, it is assumed that 80% of the welding is done by either an automatic or semi-automatic submerged-arc weld process and the remaining 20% is done manually with shielded electrodes. The chart also shows the cost effect by increasing the number of pieces required to produce the weldment. Similar charts should be made available to show cost of ordinary gray cast iron, nodulated cast iron and riveted construction versus welded construction.

In Fig. 2 it is important to note that as the weldment reduces in weight, the cost per piece on the pound basis increases more rapidly than the casting. Further, as the number of pieces to make the weldment increases, the cost of same increases at a greater rate and for this reason care should be exercised that maximum functionability is obtained from each piece being used. Also for this reason care should be exercised that the use of castings, pressings or forgings for component parts be investigated.

When manufacturing a line of products covering a wide latitude of duty or capacity, there is a tendency to use the identical design throughout the complete line. This approach generally produces either a poor profit or a loss of profit on the smaller units of the product line. The smaller units should be studied from a product-performance standpoint and, if possible, redesigned to reduce the number of pieces by combining pressings, forgings and steel castings to produce an economical composite structure.

For fabricating rectangular or other straight-sided plates, shearing is recommended through $^5/_8$ in. thickness and torch cutting on heavier plates. Shearing on heavy plates causes distortion in directions transverse and longitudinal to the plate, resulting in poor weld-joint fit-up. When castings or forgings are used, the weld-joint preparation, where required, should be incorporated on these parts.

For lowest weldment cost, the design should allow the shop the maximum possible opportunity to adjust dimensions before welding parts which have controlling surfaces. These parts should be adjusted after the major portion of the welding at the control area has been completed. In this way minimum straightening and machining cost will be involved. Further, the design should allow for maximum possible fit-up tolerance on the various parts. Figure 3 illustrates a bedplate design consisting of a channel frame and two pedestals. The sketch illustrates the locations where adjustment is allowed for tolerances on fabrication and welding to decrease the trimming, fitting, straightening and machining labor required.

In welding, the joints at the junction of the pedestal parts with the top flanges of the channel frame are made first, after which the clamps on the channel iron frame are retightened and the channels are welded at the junctions of the lateral and longitudinal members. Next, the welds joining the two pedestal pressings are made after which the pedestal foot pads are adjusted and the joints between the pedestal walls and the foot plates are made.

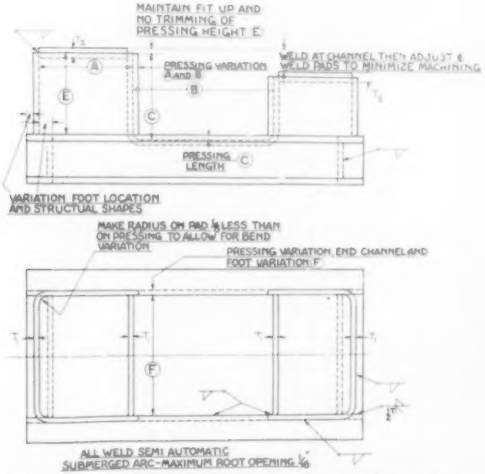
MAINTAIN FIT UP AND NO TRIMMING OF PRESSING HEIGHT E
PRESSING VARIATION A AND B
WELD AT CHANNEL THEN ADJUST & WELD PADS TO MINIMIZE MACHINING
PRESSING LENGTH C
VARIATION FOOT LOCATION AND STRUCTUAL SHAPES
MAKE RADIUS ON PAD ½ LESS THAN ON PRESSING TO ALLOW FOR BEND VARIATION
PRESSING VARIATION END CHANNEL AND FOOT VARIATION F
ALL WELD SEMI AUTOMATIC SUBMERGED ARC-MAXIMUM ROOT OPENING 1/16

Fig. 3 Suggested bedplate design to provide maximum tolerance for fit-up and also making use of the power-press brake parts to minimize welding

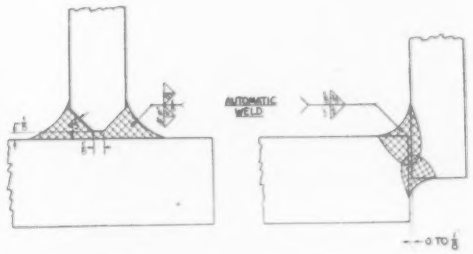

Fig. 4 Joints designed for manual and automatic welding

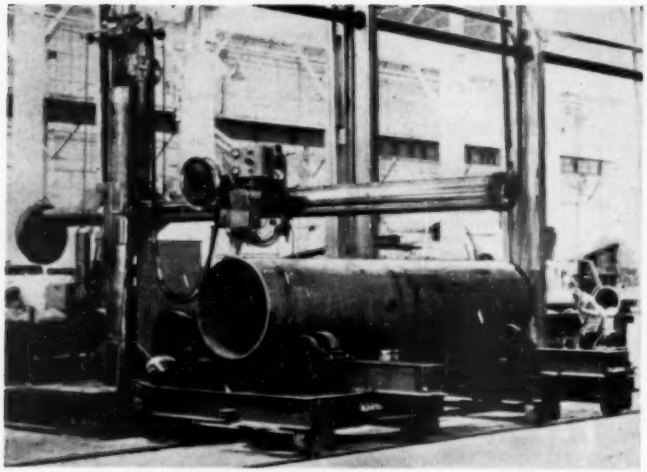

Fig. 5 An installation employing a welding-head manipulator and turning rolls to completely weld all longitudinal and circumferential welds in a vessel with one set-up operation

Another important observation is to note that a minimum number of plate thicknesses is specified. This reduces the number of plates that must be handled to produce all the elements required. Further, observation should be made that all welding is accomplished from the outer side of the weldment and, therefore, by using a welding positioner, all welds can be easily reached with minimum effort and time. This assembly can be completely welded by use of the semiautomatic submerged-arc process at welding speeds of 20 to 30 in. per minute and an operating factor of approximately 40 to 50%. By positioning joints so that the poor fit-up at the joints is placed on the vertical leg of the fillet welds, root openings up to $^1/_{16}$ on $^1/_4$-in. plate are easily welded without arcing through the joint. Use of the semiautomatic or automatic welding head, allowing high weld speeds, produces considerably less distortion than that obtained by manual processes and also practically eliminates the weld-cleaning operation.

Figure 4 (*a*) illustrates a tee joint made by welding a flange, in a conventional manner, by the manual arc process where the maximum joint speed will be below $^1/_2$ in. per minute. Figure 4 (*b*) illustrates this same joint which can be produced by the automatic submerged-arc welding process at joint speeds up to 16 in. per minute. For the latter, the use of a twin electrode automatic welding head allows use of root openings similar to those encountered in joint practice shown in Fig. 4 (*a*). This head is made by modifying a standard automatic submerged-arc welding head (including controls) which can be adjusted to produce an arc-pool effect which will wash across a joint rather than penetrate through it. Depth of penetration can be obtained by increasing amperage and, because two electrodes are available, the rate of deposition and, therefore, weld speed is increased. On plates over $^3/_4$ in. substantial root openings can be tolerated without necessity of back-up provisions. In addition to the gain in welding speeds, the design in Fig. 4 (*b*) entails less cost in joint preparation and also less fit-up time.

There is a tendency in the machine tool and other functional machinery industries to specify corner welds for purpose of appearance. A corner weld, especially on plates under $^1/_2$ in. thick, is a difficult joint to set up and weld and generally requires considerable grinding because a smooth surface is necessary due to the high-light appearance focused on this type of joint. For semi- and fully automatic weld application to these joints, it is necessary to provide either sufficient material at the root of the joint by lapping plate edges or by providing other suitable back-up means. Flux-supporting troughs must be provided at the welding-arc side of the joint and can be produced by tack welding two

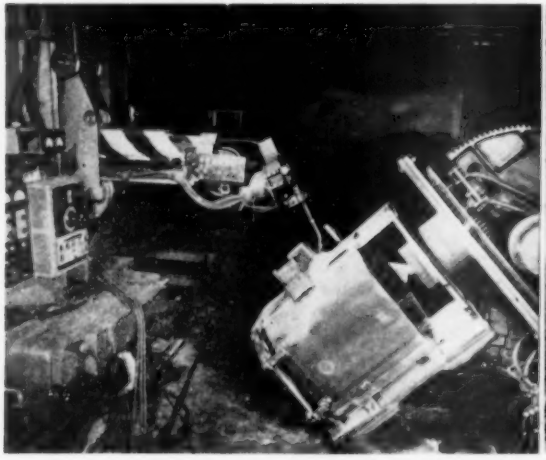

Fig. 6 Automatic welding-head manipulator for universal application to small welded assemblies produced in small quantities. Longitudinal and transverse welds are being made on the motor housing in the installation. Circular welds can also be made with this equipment

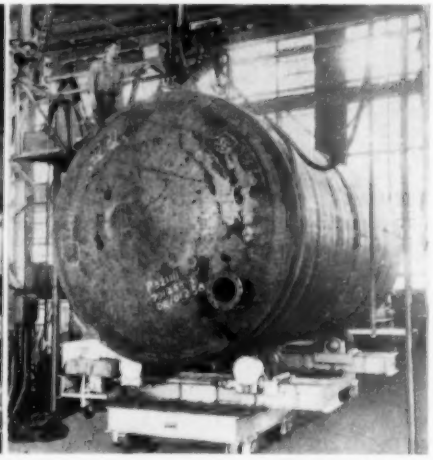

Photo Courtesy Westinghouse Electric Corp.

Fig. 7 Installation employing powerized turning rolls and propelling equipment for universal application to automatically weld various size vessels

Fig. 8 Platform-type rotating positioner to suitably position any irregular assembly for automatic welding. Rotation is available on the platform table as well as on the positioner headstock

straight edges of suitable lengths to cross ribs and then placing the straight edges to straddle the joint being welded.

Figure 3 illustrates a structural design whereby the outer corners of the unit are produced with press operations and the internal joints are produced by lapping plates to provide for fillet welds. In this manner minimum demands are placed not only on welding and fit-up operations but also in fabrication operations to effect maximum economy.

Automatic welding can be easily adapted to job-shop production if designs are produced where the major share of the welding can be applied from one side. In most cases steel or copper back-up bars, as well as fiber glass sleeving or tape can be held in position by screw clamps, air clamps or permanent magnets to accomplish complete penetration to joints requiring same. Heavy glass tape is very useful for sealing poor fits between copper back-up bars and the plate to be welded.

SHOP APPLICATION

The design approach, previously discussed, will enhance the use of universally applied jigging. For manipulating this jigging as well as the product being produced, properly designed welding positioners, power turning rolls and welding-head manipulators can be purchased as standard equipment. The latter can be used to advantage to decrease initial tooling cost, provide for versatile application to several jigs and other production as well as producing the proper environment to meet the characteristics of the welding processes and methods.

Figure 5 illustrates an installation where all longitudinal and circumferential welding on vessels can be accomplished in one setup. The vessel is supported and rotated by a standard motorized power turning roll and idler roll and, further, each of these rolls is supported on a railroad-type motorized dolly and idler dolly, respectively. The welding head is supported by a cantilever-type automatic welding-head manipulator. The column supporting the boom can be rotated 360° so that set-up work can be in progress by a set-up gang at one side of the manipulator while welding is being accomplished at the other side. Other set-up or finish-welding gangs can remove assemblies from fixtures and thereby enable the automatic welding-head maximum production capacity.

Figure 6 illustrates a more flexible and universally applied welding-head manipulator and positioner. In this figure a motor case is being welded and joints produced are longitudinal and lateral to the welding-head manipulator. To produce lateral joints, the boom on which the welding head is mounted is moved through the column of the welding-head manipulator so that the welding head traverses the joint. For longitudinal joints the welding-head manipulator is propelled along the track. In both instances the welding positioner places joints for proper control of the arc and arc pool. Circumferential welds can also be made with this equipment by rotating the assembly by means of the welding positioner. Further, inside and outside joints can be welded where there is

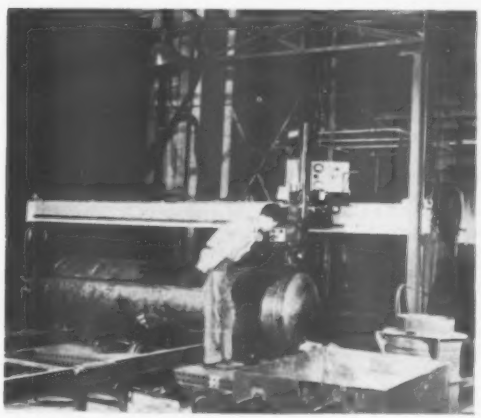

Fig. 9 Welding gantry which with a power and idler roll is suitable for welding girth and longitudinal welds on vessels

Fig. 10 Small assemblies can be manually welded to advantage by use of a positioner. Proper weld position in conjunction with the weld operator's dolly provide assurance of minimum operator fatigue to produce maximum production and high-quality workmanship

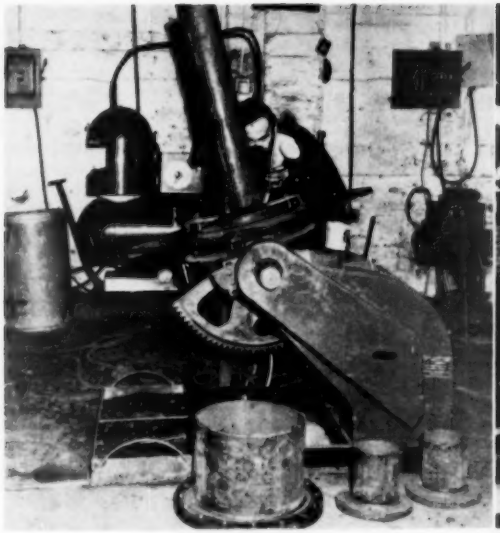

Fig. 11 Semiautomatic welding of small seamless tubes and pipes to flanges. Three assemblies previously welded are shown in the foreground

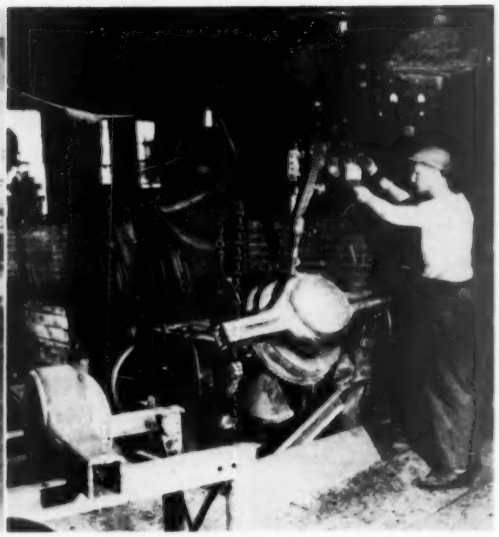

Fig. 12. Automatic welding small assemblies such as the automotive differential housings can be accomplished with welding-head manipulator and weld positioner

sufficient provision to engage the welding head. In cases where the amount of welding warrants application of a flexible electrode extension, joints several feet long can be made through openings as small as 4 or 5 in. in diameter.

Figure 7 illustrates another universal set-up for welding vessels with an automatic welding head. The welding head is mounted on a ram which can be raised and lowered. The vessel is supported by a motorized power turning roll and an idler roll, and each of the latter is supported on a railroad-type motorized dolly and idler dolly, respectively. The dollies can be spaced for any center distance to support various lengths of vessels. Remotely operated electrical tachometers assist in maintaining accurate control of the welding speed.

The power roll and the motorized dolly supporting this unit are propelled by a common transmission and by shifting two lever-operated clutches; the drive is shifted from the power roll to the dolly drive, or vice versa. For circumferential welds, the dolly drive is engaged to position the joint and then the drive is transferred to the power roll for rotation during welding. To weld longitudinal seams, the power roll is driven to position the joint under the welding head and then the drive is transferred to the dolly to propel the work for welding.

Figure 8 illustrates a platform type of positioner. The platform is mounted between a motorized headstock and idler tailstock. The hinged joint between the platform and pedestals mounted on the head- and tailstocks provides for expansion and contraction to maintain alignment. The platform is provided with a motorized rotating table and, consequently, practically any intricate assembly can be completely positioned. This unit operating with an automatic welding-head manipulator, illustrated in Fig. 6, could be applied to any irregular assembly for speedy universal application.

Figure 9 illustrates another universal welding manipulator which, in conjunction with a power turning roll and idler, can be used to readily weld various lengths and diameters of vessels in one setup. The vessels shown are generally of lighter gage plate (from $^1/_8$ through $^3/_8$ in. thick) and are provided with steel back-up strips on both girth and longitudinal joints. The complete vessel is welded in one setup and then rolled down the ways to a testing station. When using power rolls for automatic welding, it is advisable to make use of rubber-tired rolls or provide other positive drive mechanism. In welding soft alloys care should be taken to provide rolls of sufficient width to prevent high pressures which could scuff the drum surfaces. For accurate control of weld bead and penetration it is important that the rolls are level and in alignment with the travel of the gantry. To control the arc pool and slag on a large weld size application, it may be desirable to have alignment of rolls so that the joint is being welded slightly uphill and, therefore, adjustment should be provided between the turning rolls and the turning roll supports. By welding slightly uphill molten flux is kept from flowing ahead of the arc pool to assure proper penetration, weld soundness, control bead size and weld reinforcement.

Figure 10 illustrates use of smaller positioners used to position small parts. By elevating these positioners, it is possible to provide the most desirable welding position for the operator to assure increased production and proper weld quality. A dolly-type chair for the weld operator increases his mobility with minimum effort.

Simple semiautomatic welding operations can be produced by merely using a welding positioner to rotate the work past an operator. As shown in Fig. 11, the operator supports the semiautomatic gun to complete the weld. Three samples of completely welded fittings are shown in the foreground.

Figure 12 illustrates a relatively small assembly (motor-truck differential case) being welded with the automatic welding head. The welding head can be mounted on a cantilever boom and by a combination of propelling the head down the boom and rotation by means of the positioner, the complete longitudinal and circular welds can be made in one set-up operation.

In the job shop, tooling has a substantial effect on the cost and success of the weldment. Consequently, the tooling must not only produce a fast and accurate setup, but also must be suitable for modification by removing or adding elements to accommodate a full range of sizes and types of products being produced.

Development of Fused Metallized Coatings

A review of the investigations conducted in the development of materials, techniques and procedures for the application of fused metallized coatings

by Harrison S. Sayre

INTRODUCTION

DURING the past few years the increased use of hard facing and corrosion-protective coatings has contributed greatly toward lengthening the life and reducing the number of failures in machine parts, structures and welded assemblies subject to wearing, corrosive or erosive influences. The application of overlays has become an important item in the Armed Services as well as in industry. The U. S. Naval Engineering Experiment Station has played an important part in the development of hard facing applications for use in the Naval Service.

OVERLAY IN THE NAVAL SERVICE

The Navy has conducted extensive investigations in the search for a suitable coating for low-alloy steels to allow their use in salt water. Two applications in particular have created considerable interest along this line. These are the desire to use steel propellers and the improved protection of outboard propeller shafting. Both of these applications require complete protection of the base metal as a means of preventing corrosion fatigue.

In the case of propellers a considerable saving in cost and in strategic materials would be realized by the use of steel in the place of the usual manganese bronze. The design trend toward thinner blades and higher operational speeds has required that materials stronger and more resistant to cavitation be used. The coating of a steel propeller has been a problem on which the Navy has been working for a number of years.

The present method of protecting outboard portions of propulsion shafting is to cover the bearing areas of the steel shaft with bronze sleeves and the balance of the shaft with a neoprene coating so as to prevent sea water from coming in contact with the shaft. There is, however, a possibility of incomplete protection at the joints in the sleeves or where damage occurs to the neoprene coating. This is one reason for operating Naval vessels with relatively low shaft stresses and correspondingly large shaft diameters. Advanced design practices are now employing lighter weight, higher stressed shafting. With these conditions the reliability of the protective covering becomes extremely important. With the higher stresses and increased twisting of the shaft the bronze sleeves and neoprene coating may not function so satisfactorily. It is for these reasons that the Navy has been interested in developing the use of metallizing and remelting as a means of applying a protective coating to shafting.

The use of hard surface and corrosion protective overlays is not by any means limited to the above two applications. There are literally hundreds of machine parts and assemblies in use throughout the Naval Service where the application of a protective coating contributes greatly toward increasing the life of the part and affects appreciable savings in cost through the reclaiming of damaged or worn equipment.

METHODS OF APPLICATION

The above demands for methods of application of overlays to propellers started a number of years ago with a basic study of the various methods for the application of hard-surface or corrosion-resistant coatings. They are gas welding, electric-arc welding and recently by metallizing and remelting.

In the gas-welding procedure the overlay is applied to the base metal by means of a torch and a rod of the overlay material. The main advantages of this method were found to be the absence of dilution of the base metal and resultant maintenance of the characteristics of the overlay material.

Fig. 1 Overlay applied by gas welding

This is an important factor as the compositions of the base metal and overlay are generally quite different. Another important factor is the ease with which thin layers of surfacing may be applied and the ease of running the surfacing out to sharp changes of contour in the base metal. A typical gas-welded overlay is shown in Fig. 1.

The main advantages of overlay deposition by electric-arc welding methods lie in the high rates of deposition attainable, flexibility of application and ease of mechanization. Arc welding is also especially applicable to localized surfacing of large or heavy pieces. An electric-arc-welded overlay is shown in Fig. 2.

Recently there has been developed a companion method for the application of overlays. The application of coatings by this procedure, metallizing and remelting, was started by U. S. Naval Engineering Experiment Station personnel a number of years ago. The AMERICAN WELDING SOCIETY has adopted the term fused metallized coating to describe this procedure. In this method a controlled thickness of the overlay material is first applied by metallizing using a metal

Harrison S. Sayre is a Welding Engineer with the U. S. Naval Engineering Experiment Station, Annapolis, Md.

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This paper presents the author's personal opinions and assertions, which do not necessarily reflect the official attitude of the Navy Department.

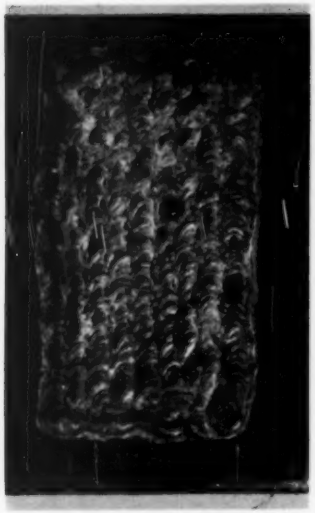

Fig. 2 Overlay applied by electric-arc welding

Fig. 3 Overlay applied by fused metallized coating

spray gun. Following application in this form, the coating is remelted to form a homogeneous fused metal overlay by means of an oxyacetylene torch, controlled atmosphere furnace or induction heating. The principal advantages of this method are that the application is simple, comparatively low heat input is required, and distortion is held to a minimum. A thin controlled thickness of overlay can be applied and subsequent machining or grinding reduced to a minimum. In many cases the metal as deposited is sufficiently smooth and no finishing operation other than polishing is required. A surface coated using this method is shown in Fig. 3.

APPLICATION PROCEDURE FOR FUSED METALLIZED COATINGS

The application procedure for the overlay of a surface with a fused metallized coating is relatively simple although development of the optimum techniques has brought out many precautions which must be observed in order to produce a sound overlay. The basic steps in the procedure consist of the following: A shaft specimen illustrating these steps is given in Fig. 4.

1. The surface to be coated must be cleaned to remove all grease, oil or other contamination. All scale must be removed and there must be no pits or holes containing sources of contamination. Sharp changes in contour of the surface must be avoided.

2. Grit blast the surface using sharp, clean, angular No. 30 steel grit. This gives a roughened surface to provide a mechanical bond for the sprayed-on particles of overlay material. Following blasting, the surface should be protected to prevent contamination or oxidation prior to the metallizing process.

3. Spray the overlay material on the whole surface in thin even coats. The first layer should be applied as quickly and evenly as possible to prevent formation of surface oxides due to heat imparted by the sprayed material and flame of the spray gun. This metallizing may be accomplished either with the material in powder form and a powder-gas metallizing gun or with the material in wire form and a wire-gas-type gun.

4. Remelting of the sprayed-on material should be accomplished as soon as possible after spraying. When remelting by means of an oxyacetylene torch, the whole surface to be remelted should be preheated to approximately 700° F. Care must be taken not to allow this preheat to exceed 1000° F. as there is danger of oxidation of the base metal below the overlay. If oxidation occurs, a satisfactory bond will not be obtained. To remelt, the flame should be directed at the metal until the overlay assumes a glassy appearance. As this point is reached the remelting torch should be advanced resulting in a progressive fusing of the entire surface.

5. After remelting, the rate of cooling will depend upon the type of base metal, the mass of the part, and variations in section. It has been found that on low-alloy steel base metal the part should be slow-cooled in lime or by wrapping in asbestos. However, on certain alloys such as high-carbon and nickel steels, it is necessary to furnace-cool the coated piece at a controlled rate of 50 to 100° F. per hour to avoid cracking of the overlay. After completion of the cooling period the part should be carefully inspected for cracks or defects in the coating. At the Experiment Station the oil-powder method of flaw detection is used for this inspection.

PROPELLER COATING

The initial phases of the investigation to develop a suitable corrosion and cavitation-resistant coating for steel propellers were started in 1942. The metallizing

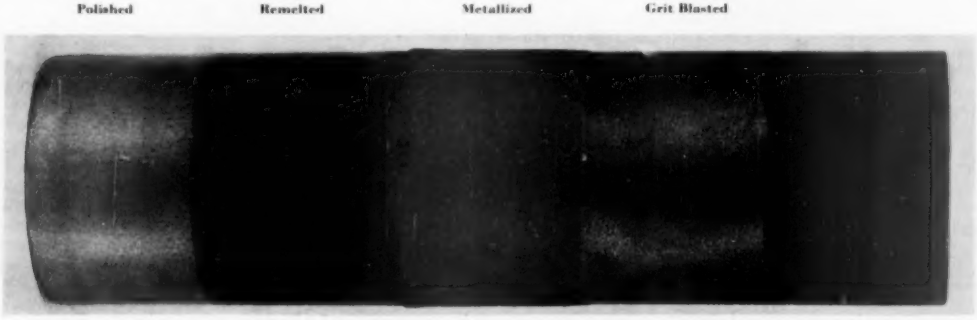

Fig. 4 Specimen illustrating steps in application of fused metallized coating

and remelting procedure was first applied using phosphor bronze as the overlay material. The optimum cladding technique was developed and this procedure was applied experimentally to two small propellers. One of these, Fig. 5, fabricated of mild-steel plates and cladded with phosphor bronze, was operated in Severn River water for approximately 1200 hours. During this period no defects or failures developed. However, cladding steel with phosphor bronze was found to entail certain disadvantages which would render the application difficult and impractical from a production standpoint.

Fig. 5 Steel propeller clad with phosphor-bronze coating

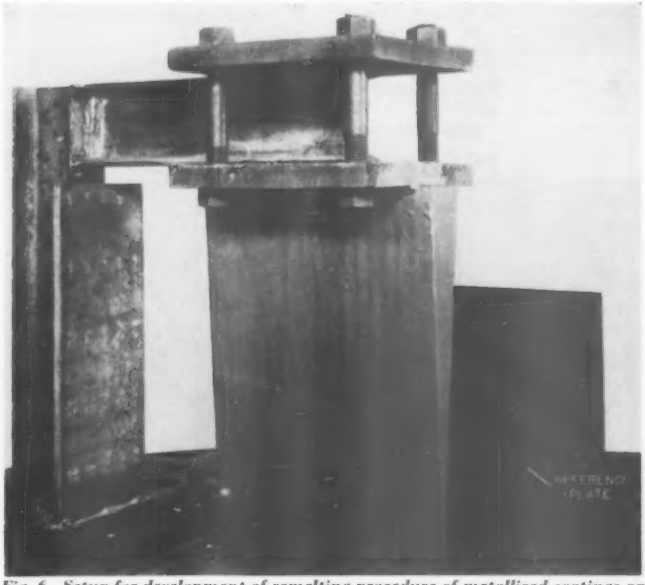

Fig. 6 Setup for development of remelting procedure of metallized coatings on propeller blades

In the continued search for a satisfactory protective coating to be applied by metallizing and remelting many metals and alloys were tried. These included nickel, brass, monel, stainless steel and nickel-chrome-tungsten alloys of various types. None of these materials were found suitable for use in fused metallized coating procedure.

Recently a chrome-nickel-boron surfacing alloy was developed. This material, available in either powder or wire form, is particularly suited for application by metallizing and remelting and has the desired properties of resistance to corrosion and cavitation. This alloy has a nominal composition of 65–75% nickel, 13–20% chrome, 3–5% boron with a combined maximum of 10% iron, silicon and carbon. A test program for the evaluation of this material for use in surfacing of steel propellers was initiated the latter part of 1947.

In the coating of a steel propeller various problems are present which required the development of special techniques. Extreme care must be taken to eliminate warpage of the blades during the cladding procedure. In order to control distortion, a technique was developed wherein the blade is held in a vertical position and two welders are employed to simultaneously remelt both sides of the blade. Figure 6 shows a simulated blade and setup used in the development of this procedure. To prevent warpage by control of heat input the two welders must be trained to work as a team keeping their torches and remelting progression synchronized directly opposite each other on the two sides of the blade. Care must be taken to fuse all portions of the coating yet the heat input must be carefully controlled to prevent running or sagging of the coating alloy. A double remelting procedure was adopted as it was found that this produced complete fusion of the overlay, and a sounder and smoother deposit.

As a practical test a cast steel YMS propeller was coated employing the chrome-nickel-boron alloy as the overlay material. By use of the above-described techniques the warpage in the blades was held to a minimum and the overlay on the blades was quite smooth. However, examination of the coating showed cracks in the overlay on the hub section and small defects in the cladding resulting from inherent defects in the casting. Attempts were made to develop methods for the repair of coating defects. However, all attempts at local repair resulted in cracking of the coating in surrounding areas. This indicated a definite need for a modification of the coating alloy to permit satisfactory use on steel propellers.

A modified alloy mixture was developed which had a composition of 40 parts

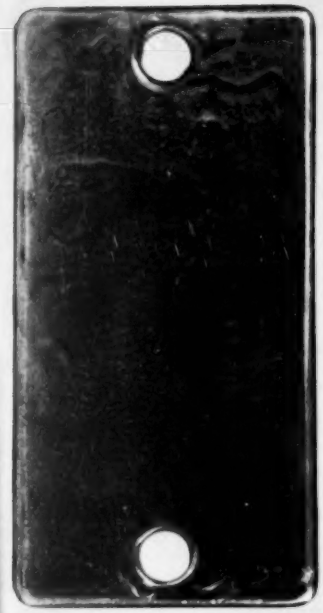

Fig. 7 Fused metallized coating on test plate following salt-water immersion test

chrome-nickel-boron basic alloy, 60 parts nickel by weight with sufficient silicon added to raise the silicon content of the total mixture to 5%. The results of a series of evaluation tests performed on this modified material were as follows:

1. The remelted coating could be allowed to cool in air without the development of cracks or defects.

2. Salt-water immersion tests showed the overlay to be corrosion-resistant and the base metal fully protected. Figure 7 shows a steel-plate specimen which was subjected to three months' immersion in Severn River water, a brackish estuary water. This specimen showed no signs of ferrous corrosion. The plate was washed to remove marine growth for closer examination of the remelted coating.

3. In the event defects are found in the coating or if it becomes damaged in service, the coating can be repaired by locally remelting the area or by grinding off the damaged area, respraying and remelting the sprayed mixture.

4. The hardness of the modified material averages 41 Rockwell C scale. This is approximately 15 points below the basic alloy; however, for corrosion protection, hardness is not of prime importance.

Additional practical evaluation of this procedure as a means of protecting steel propellers is planned in connection with the rehabilitation of the damaged steel blades of an ice-breaker propeller. This program has been initiated, the damaged areas in the blades repaired by welding and the application of the overlay alloy is scheduled for the near future at a West Coast Naval shipyard.

This procedure also merits consideration in the event of future rehabilitation of the wartime merchant fleet in view of the scarcity of copper and realizing that approximately 60% of each 15-ton merchant-ship propeller is copper.

PROPULSION-SHAFT COATING

The application of a metallized and remelted coating to shafting is simplified due to the geometry of the part. In most instances the shaft can be rotated constantly during the entire procedure and a smooth controlled-thickness coating may be quickly obtained with no resultant distortion of the shaft. The basic technique is similar to that in the case of flat surfaces, that is, the shaft is cleaned, grit-blasted, metallized, remelted and finally polished, machined or ground depending upon the final condition desired.

The technique employed in the remelt of the metallized overlays on round bar stock plays an important part in the production of a sound coating. Sufficient heat input must be provided to permit complete fusing of the coating as rapidly as possible. As a practical example, in the remelt of a 0.020-in. thick chrome-nickel-boron coating on 3-in. diameter stock, two oxyacetylene torches with No. 13 bulbous-type tips are employed. The flames of these torches are positioned 180° apart on the circumference of the shaft with one torch slightly advanced in position longitudinally in the direction of remelt. The shaft is rotated at 100 rpm. throughout the remelt process. The time required for a double remelt of the coating is approximately $2^1/_2$ min. per 6-in. length of shaft. The importance of employing trained operators cannot be overemphasized as the rate of remelt and soundness of resultant coating depend in a large measure on the ability of the operator to determine the point at which fusion of the coating is reached.

The application of the chrome-nickel-boron overlay alloy to low-alloy steels presents very little difficulty. Sound deposits are obtained without cracking or checking of the surface. However, with high-carbon and alloy steels, such as Class An, $3^1/_2$% Ni steel and Alloy No. 4 steel, difficulty is encountered in obtaining a coating free of cracks. Figure 8 shows an Alloy 4 hollow steel shaft which cracked during cooling. These cracks are due to the differences in the coefficients of expansion of the base metal and overlay material and to the relative speed of contraction during the cooling period. It has been found that crack development occurs not only during the cooling period but frequently after an appreciable lapse of time. It may even occur during the storage of a finished shaft. Figure 9 shows a shaft in which no cracks were present immediately following the cooling period but which developed the cracks shown within 24 hours. Cracks are shown by oil-powder flaw detection. In some services minute cracks may be of minor importance and have very little influence on the life of the part. In services involving corrosive action their elimination is of primary importance.

By development of improved remelting techniques and controlled cooling of the shafting following remelt, shaft specimens were obtained which were free of cracks even following a prolonged storage period at room temperature. However, when these specimens were immersed in salt water, cracks began to develop in the coating following immersion periods ranging from 12 hours to 5 days. Figure 10 shows a shaft in which the cracks shown developed after 18 hours immersion in salt water. This shaft had been kept in storage at room temperature for three

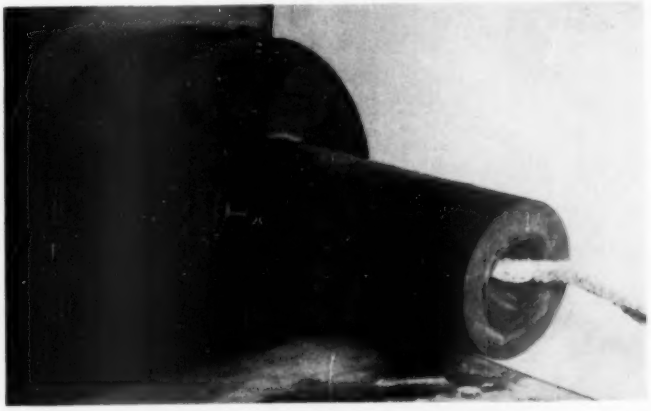

Fig. 8 Fused metallized coating cracked during cooling period

weeks prior to the immersion test. During the storage period no defects developed in the coating. There is apparently a high order of residual circumferential tensile stress in the coating. The coating is subject to a stress corrosion when immersed in the salt water as a corrosive medium.

This condition of stress corrosion which renders the basic chrome-nickel-boron alloy unsuitable for use on propulsion shafting led to the adoption of a modified coating material as previously described in connection with propeller cladding. The use of the modified material resulted in a coating which was free of crack development during the cooling, storage and salt-water immersion periods to which it was subjected. Figure 11 shows a Class An, 3½% Ni steel shaft clad with the modified alloy. This specimen was immersed in Severn River water for over 4 months. Following removal from the water, the shaft was washed to remove all marine growth. Inspection showed no visible signs of ferrous corrosion and the coating was free of cracks.

This procedure has not been found acceptable for the new combat vessels which will employ shafting having higher tensile strengths. The procedure is applicable for the protection of outboard shafting of the smaller auxiliary vessels employing shafting made of steels whose tensile strength is from 60 to 80,000 psi.

Fig. 9 Fused metallized coating cracked during storage period

Fig. 10 Stress corrosion cracking of fused metallized coating

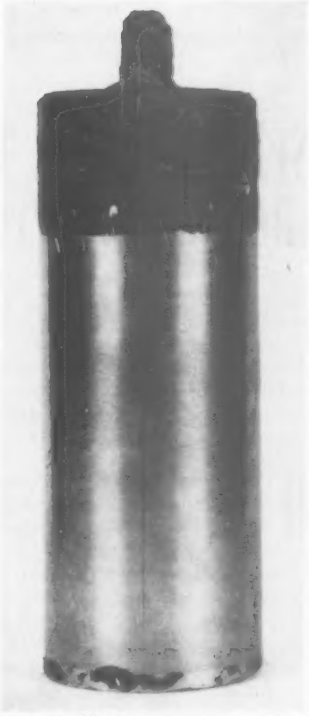

Fig. 11 Modified fused metallized coating following salt-water immersion test

OTHER APPLICATIONS FOR FUSED METALLIZED COATINGS

This procedure has many applications other than on propellers and shafting.

The basic chrome-nickel-boron alloy, due to the hardness of the remelted coating is particularly suited for the maintenance of production tools and gages.

Recent reports received from various manufacturing firms have revealed some startling results on the savings affected through the salvage of worn parts by the use of fused metallized coatings. In one example, mandrels were salvaged. The mandrels made of the best tool steels and chrome plated lasted only about 75 operations. Mandrels coated with a fused metallized hard-surface alloy, have been in use for more than 35,000 operations with no visible signs of wear.

The Bureau of Aeronautics has applied this procedure to increase the life of arrester hooks on carrier based aircraft.

CONCLUSIONS

The use of fused metallized coatings for hard surfacing and protective overlay is still relatively new. Although the employment of this procedure has not been fully exploited by either industry or the Armed Services, new applications are constantly being discovered. A great deal of the success in the coating lies in the development of the optimum techniques of application for each type of base metal and overlay material. Knowledge of the capabilities of fused metallized overlays to effect savings in cost, time and strategic material would be promoted by a more widespread exchange of information on this procedure.

ACKNOWLEDGMENTS

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Selecting and Training Welding Operators for the Defense Program

Methods of training welders for manual welding in the arc, gas and inert-gas-arc processes

by A. N. Kugler

ONCE more, our country finds itself confronted with the necessity for preparing its industries for defense production. Welding, now one of the standard metal fabricating methods, is of vital importance in the manufacture of ships, tanks, planes and the great variety of war materiel. The available supply of those skilled in the art of welding, while adequate for peacetime operations, falls far short of defense-production needs. Thus, again, it becomes necessary to increase the supply of welders by various training programs. The art and science of welding have advanced materially in recent years. Training concepts, adequate for World War I, were inadequate in the second great conflict and similarly now a newer approach in the fields of the new inert-gas welding processes is necessary.

In order to keep this paper within bounds and avoid some of the complexities of the automatic processes, this discussion will be limited to the training of welders for manual welding in the metal-arc and oxyacetylene processes and some of the newer inert-gas-arc processes.

In studying this problem of welders for defense production, it seems appropriate to start with an analysis of available welders and the regularly established means for training welders. In the currently available supply of welders, we have those who are regularly employed in industry in normal peacetime production. This group, while limited in number, and adequate for normal times, falls far short of those needed for expanded defense production. In addition to this basic supply pool of welding labor, there is another group not engaged in welding but who have received welding training during World War II. These are the people, from other walks of life, who for one reason or another were compelled to enter war production, and selected welding as their field. The degree of skill in this group is a function of their original training, their native skill and the length of time they have been away from welding operations. The training will range all the way from those who received complete welding courses down through those who trained for specialized operations, even as limited as tack welding.

To supplement the group of regularly employed welders, the only sources of supply we now have are the vocational schools teaching welding and those industries which have established their own training programs to assure a continuous supply of welders. Obviously, new recruits from this source are extremely limited and could hardly fill the requirements of expanded defense production. Another significant factor in this phase of the problem is the great decline in the number of vocational schools teaching welding.

To meet this increased demand, it will be necessary to expand the training facilities both within industry and in established vocational and trade schools. Expanding industry training programs may be somewhat simpler than for vocational schools because industry can draw upon production welders for instructors. Further, industry, by the use of suitable training programs, can use the trainee for limited, minor production welding operations. Increasing the facilities for training may present some problems if building space is in short supply. In general, however, industry has better means of proceeding in this program than do the trade schools.

The vocational and trade schools, whether operated with public funds or as private enterprise, are limited in their budgets and in their facilities. Further, they do not have the sources of supply for instructors. Generally speaking, teaching in vocational schools does not pay as well as working as a welder in industry. Therefore, there is little incentive for a regularly employed welder to give up his job to take a teaching position. Recognizing these problems confronting the trade schools, it must be apparent that outside financial help is necessary if trade schools are to expand their operations. During World War II, such help was supplied through the U. S. Office of Education and the various State educational bodies. The program included not only plant expansion but also provisions for instructors.

A. N. Kugler is connected with the Air Reduction Sales Co., New York, N. Y.

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Another source of facility for welder training is schools and colleges which formerly may or may not have taught welding but which have shop facilities which may be adapted to the teaching of the welding art. This, again, was done in World War II, and should provide a very satisfactory source of facility. Again, however, the problem of finances and man power is critical because such institutions, of themselves, are not able to underwrite the cost of such expansion. Outside help must be available from government sources to provide the wherewithal to place these institutions in a position to take their share of the teaching load.

Thus, the facilities for teaching have to be worked out in each area on the basis of available supply of men and facilities. Established schools should be utilized to the fullest extent as far as possible before going to the extreme of building new facilities. Instructors for these expanded courses may some times be drawn from industry on a part-time basis or from the welding-supply companies, also on a part-time basis. Very successful training courses were operated during World War II as evening programs and the instructors were brought in from local industries and welding-supply companies.

There has been much discussion in the past as to what constitutes an adequate program for training the welders. Much of this discussion could have been eliminated if there had been prior agreement on certain fundamentals. If the objective of the program is to produce a fully qualified welder capable of performing all the operations in a given process, then obviously, the course should be as complete and thorough as possible in all aspects. Such a course would take an extended period of time. If it is desired to produce thoroughly competent welders, capable of welding with several processes, e.g., gas and metal-arc welding, a really protracted course is necessary, one taking several months to several years. Such courses are of the type commonly given in peacetime in the well-equipped vocational school. Those successfully completing such courses, plus an adequate period of practical welding may be considered as all-around, fully trained welders.

In the defense speed-up, there is neither time nor need for such elaborate courses. However, the fact that a shorter period of instruction may suffice, should not permit the thinking to become clouded to the extent that the course be shortened so much as to produce insufficiently trained welders. The defense course should be "tailored" to suit the needs of industry in the area. If possible, it should provide for a minimum of training which will enable the candidate to acquire sufficient skill to enable him to get into production as quickly as possible. Such minimum courses should be supplemented with up-grading programs which will permit the beginner to expand his skill and render more valuable service. Such a program has the advantage of utilizing practical welding work to supplement classroom training.

The objective of these courses should be to impart to the trainee sufficient manual dexterity to permit him to perform normal welding operations required. Such dexterity can only be achieved through continuous practice. Therefore, it must be obvious that the school must have adequate equipment and sufficient supplies in base metal and filler metal to permit practice.

The course should also provide the trainee with sufficient understanding of the materials and equipment with which he is working so that when out in the production line, he will be able to perform intelligently and efficiently under supervision. Much of this knowledge may be imparted in the welding booth. At least a portion of it must be put across in a classroom in the form of lectures on related theory. Related theory must be properly balanced to provide a well-rounded course. It must not become the sole purpose of the program, nor may it be completely ignored. Ideally, the lectures on related theory should be coordinated with the shop program. This may be difficult to achieve in a defense program, particularly if trainees are permitted to enter or graduate at any but regular, stipulated times.

Safety in the performance of welding operations is of paramount importance. The fact that defense conditions necessitate a speed-up in training cannot be accepted as an excuse for neglecting or omitting instructions in safe practices. Therefore, one of the most important phases of the lectures on related theory is safety in welding and cutting. Any course which omits this feature must be considered as grossly deficient. One might even say that safety is more important under defense training conditions! In this connection, it seems appropriate to call attention to the American Standard, Safety in Electric and Gas Welding and Cutting Operations, A.S.A. Z49.1-1950.

While on the topic of safety, it seems appropriate to mention briefly the handling of, and facilities for distributing oxygen and acetylene in welding schools. These gases are commonly used in arc-welding courses as well as the gas-welding courses. Experience gained in World War II training activities has demonstrated quite conclusively that manifolding the gas supplies and piping them to individual student stations, will promote safety operations in the welding school. An added feature is increased economy. Such manifolding and piping systems must be installed in accordance with the principles set forth in the National Board of Fire Underwriters, Pamphlet No. 51, and the Safe Practices for Installation and Operation of Oxyacetylene Welding and Cutting Equipment published by the I.A.A.

Early in World War II, the AMERICAN WELDING SOCIETY, through its Committee on Minimum Requirements on Instructions for Welding Operators undertook a program, developing a series of codes covering the various phases of welding operations. The first code to appear was Part A and covered the Arc Welding of Steel in thicknesses $^3/_{16}$ to $^3/_4$ in. The second code to appear was Part B-1, Oxyacetylene Welding of Steel—Aircraft, and covered thicknesses up to $^3/_{16}$ in. In preparation is a Part A-1, Arc Welding of Steel Aircraft and Part B, Oxyacetylene Welding of Steel, $^3/_{16}$ to $^2/_4$ in. thicknesses.

These codes are primarily pointed toward the complete program of welder training in the field designated. As such, they are comprehensive courses which require an appreciable length of time to complete. However, these same codes may be adapted for the short-time, intensive, defense training program by selecting appropriate sections. It is a serious mistake to apply any given code *in toto* to a specialized training program. In short, intelligent selection is needed to apply these codes for specialized purposes.

These codes comprehend two phases of the welding program, namely, the basic practical welding exercises and the related theory. In the case of the exercises, frequent use is made of check tests to determine a trainee's progress. By utilizing check tests instead of time limits on a given exercise, a more proficient trainee is not held back, while the slower-to-learn is given the time to acquire the necessary skill.

At this point, it seems appropriate to point out that an adequate course in welding—either comprehensive or short-time—requires that a group of exercises be organized to carry the trainee from his starting level of skill up to the desired advanced stage. One thing which must be avoided is the mere welding of "test plates" or concentration on qualification tests. Unfortunately, during World War II training, such abuses were practiced by some schools which guaranteed a prospect "to pass the test after taking the course." Obviously, one trained under such a system has little knowledge and background of real welding techniques. Adherence to the A.W.S. Codes of Minimum Requirements will go a long way toward elimination of such poor practices.

A little earlier in this paper, the available supply sources of those with some welding experience were enumerated. The specific program of welding training must be organized to handle trainees in these several groups and in terms of processes needed.

The novice with no previous experience of welding obviously requires more fundamental training and more extended training than those with previous experience. This must include instruction and lectures on the related theory in connection with the fundamentals of the process. It is at the novice stage that the *safety* lessons must be *strongly emphasized*. In normal peacetime welding, novice training is merely preliminary to the larger welding training programs. In an accelerated defense course, it is some times possible to bring the novice up to such a degree of skill as will permit him to perform simple tack-welding operations. Such a trainee is then permitted to work as a tack welder in production and secures additional training through the medium of an up-grading course. He has the advantage, however, in his normal workday of observing regular production operations. This feature sometimes permits of more rapid advancement in a trainee's skill.

For those "alumni" of World War II training programs, who received the limited instruction, it will be necessary to provide substantially the same type of training as enumerated above for the novice. Since their training was inadequate, it would be unsafe to permit them to advance with more involved welding operations. Somewhat related to these, are those operators who received training in a limited field, e.g., metal-arc welding with E6020 electrodes in a horizontal position. Such candidates would have at least the rudiments of the welding operation and would, therefore, be suitable material for enrollment in the up-grading courses. However, before entry in such an up-grading course, candidates in either category should be afforded a refresher course which would terminate in a suitable test to determine their knowledge and skill with welding operations in so far as they profess to know them.

The final stage in the welder training program is that of teaching to acknowledgedly competent welders a new process, e.g., inert-gas tungsten-arc welding or inert-gas metal-arc welding with consumable electrodes. This, in reality, is the simplest of the teaching operations and probably the shortest type of course to offer. The trainees, by previous experience, have acquired a background of skill; hence all that is necessary is to impart to them specialized techniques, peculiar to the processes. This, of course, will include such special safety precautions as may be peculiar to the new operations. Such training in new processes is probably best accomplished within industry since facilities for these newer techniques are not readily available to schools. However, there are a few schools successfully teaching the new processes, preceded of course, by basic training in arc welding where this is necessary.

Generally speaking, in defense training programs, it is the practice to employ the standard qualification test established for the type of work involved, as a final test. Thus, the trainee working toward employment in a shipyard would take a Navy or Maritime Commission qualification test; one looking forward to work in a tank factory would take the Army Ordnance qualification test. These tests, while identical with those administered under government contracts, are not generally acceptable as qualification for work on contract jobs. The reasoning behind this is simply that a trainee working in a school is welding on relatively small pieces in which conditions of restraint, etc., are not encountered. It is, therefore, important that a trainee, after completing his course of instruction, put in a period of work as an apprentice or tack welder under production conditions before he takes an official qualification test.

The training of welders is an important phase of our defense program. It presents many problems, some of which are new and peculiar to the present conditions and others which are substantially the same as those encountered in World War I. For industry confronted with these problems, there is a large amount of data available from the American Welding Society and from the publications of the Committee on Minimum Requirements for the Training of Welding Operators. Of equal importance, obviously not covered here, is the problem of training and education of the welding inspector. This is something for future study and action.

Tentative Standards for School Welding Shops

A guide to shop facilities for public school systems that have or are establishing welding training programs

by Carl H. Turnquist

INTRODUCTION

The training of welding operators is an exceptionally important element of the welding industry.

As a result of demand, the public school systems of the nation have established or are establishing welding training programs for our youth and for our adults.

This paper is intended to serve as a guide for those who must design and prepare the specifications for such welding shops.

The paper is limited to the construction of the shop facilities only. Such important phases as equipment and supplies must, of necessity, be reserved for some other presentation.

WELDING has become an important part of practically all industrial, vocational and technical situations.

But it is not only because welding is an asset, that all mechanically minded youth should understand it and have abilities to use it; it is also an important educational tool.

We, who have taught welding, find that, except for those who are physically handicapped, the learning of welding can be a stimulation to the individual. Welding can be advantageously used to encourage the study of English, Mathematics, Physics, Chemistry and Drawing.

Welding is an important educational device, to teach youngsters a new skill and to use it as a learning stimulus.

The largest welding problem in the public schools is the cost, both original, replacement and operational. However the original and replacement costs very favorably compare with and are usually less than the cost of school shops equipped with power tools. The operational cost exceeds practically that of all other school shops.

Regardless of the slight increase in operating cost over the other types of school shops, I sincerely believe welding, both gas and arc, should be taught to our students both on an exploratory level and as a major study.

It is necessary that the welding shop be designed as intelligently as possible to avoid inefficiencies that would otherwise deter good instruction for the life of the facility.

For the past three years the Detroit Section of the AMERICAN WELDING SOCIETY sponsored an Educational Committee. This Committee has developed welding instruction shop layouts in addition to developing many other phases of secondary-level welding education.

It is my privilege to acquaint you with some of the proposed shop layout standards as they were established by this Committee.

TYPES OF SCHOOL WELDING SHOPS

There are several possible types of school welding shops. They are:

1. The single or double station in a general metal shop.
2. The combination gas welding and arc welding shop.
3. The separate gas welding shop and the separate arc welding shop.

In all of the tentative shop designs, safety was the most important factor considered.

It is obvious that all of these types of programs present many unusual problems and considerable thought must be expended on the shop layout to expedite good educational practices. The general metal shop with one or two welding stations is used in small communities or where the school wishes to experiment with a welding training program. The one-room welding shop gives great flexibility of instruction and is used when the student load shows the need for only one instructor.

The two-room welding training facility has many advantages. It simplifies the ventilation problem in each shop; it enables the instructor to be more efficient; it enables him to better arrange his materials, tools and equipment; and it permits more thorough and detailed instruction when all the students are concentrating on one plan of welding instruction. This two-shop arrangement is practical only when there are at least two instructors and sufficient students to keep both shops running simultaneously.

Carl H. Turnquist is Head of the Automotive and Welding Dept. of Cass Technical High School, Detroit, Mich.

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The larger welding shops are planned to provide facilities for training approximately 25 students each. It is understood that practically the same facilities are needed whether the student spends 5, 7½, 15 or 30 hr. per week in the shop. It is also true that the total length of the course, whether it be 75, 300 or 600 hr., necessitates no great change in the shop layout.

The shop layouts are based only on those fundamental training units believed necessary for all types of training. More shop training time, however, will enable the instructor to teach more operations pertaining to each basic unit. An example of this practice is the gas-welded butt joint. The basic unit would have this title whereas additional time would provide for additional units of butt joint instruction in different positions, different thicknesses of metal, and different kinds of metal.

It should not be assumed that any particular school situation will be able to follow completely and in detail the recommendations in this publication; rather it is to be expected that each locality will use these recommendations as a guide in solving its own local problem.

The school welding shop designs suggested in the following paragraphs are an attempt to make the shops as safe and usable as possible at a minimum cost. Because so many of the school shop features are similar, each item needing special attention is discussed simultaneously for each type of shop.

SHOP AREA

The general school shop with one or more welding stations should provide a space of at least 4 x 4 ft. for the gas station and 5 x 5 ft. for the arc station.

The one-room welding shop should be in a separate building, if possible. This building should be on a parcel of land which permits the shop to be approximately 50 ft. from the main building. The building should have a floor space of approximately 2800 to 4000 sq. ft. The larger area is recommended to permit less crowding of equipment. These areas permit a floor space of 40 x 50 ft. up to 50 x 80 ft. The person responsible for the design must remember to allow for expansion of facilities, and also that crowding promotes accidents.

If it is impractical to use a separate building, local fire regulations must be carefully followed concerning the use of attached rooms for welding shops in which explosive gases under pressure are stored and where open flames are used.

The combination shop should use two areas each of 2800 to 4000 sq. ft. floor space. Otherwise the above instructions would apply.

BUILDING

When adding one or two welding stations to a general metal shop, the shop must be carefully surveyed to ascertain whether or not it conforms to welding safety regulations.

It cannot be overemphasized that careful planning of the building or room will eliminate years of teaching under severe handicaps. Original installations are usually much less expensive and much more satisfactory than alterations.

FLOOR

The floor in all the plans should be made of two-course concrete with an air entraining additive. It is suggested that a light-colored cement and light-colored sand be used. It is important to secure a guarantee against "dusting" (obtained by correctly mixing and curing the cement).

The floor must be strong enough to carry 200 lb. per square foot as a minimum.

Provisions should be made in the floor for ventilation, heating, plumbing and the electrical conduits.

All of the welding area should be on one general level, and it should be provided with floor drains to facilitate washing the room. Caution: Wet arc-booth floors may be dangerous. Mopping only has generally been found to be unsatisfactory in a welding shop.

DOORS AND DOORWAYS

In the event that any of the shops adjoin the main school building, any doorway connecting between the two should be fitted with a self-closing fire door.

The shops should be equipped with two exits located at opposite ends of the shop if possible. The doors leading to the gas storerooms must also be of fireproof design.

WINDOWS

The windows should be of the steel-sash glazed type with wire reinforced glass. Glass bricks may be used, but for not over 75% of the window area.

WELDING-GAS STORAGE

The provisions used to store the oxygen, acetylene, hydrogen and inert gases must conform stringently with the local building and safety codes. These cylinders should be stored in fireproof individual rooms. To enable easy handling of the cylinders, the storeroom floor should be on the same level as the shop.

A loading and unloading facility must be provided to permit the shipping and receiving of these cylinders without difficulty.

As a safety precaution, neither the welding shops nor the storage rooms should have classrooms located above them. It is also important to have all these facilities at ground level to prevent accumulations of escaping gas. It is best to eliminate any basement or sunken cavity in which gases may collect.

Skylights are not recommended. Windows of the fixed type are preferred, provided adequate mechanical ventilation is used.

The east, south and west exposures of the shops are best fitted with heat-reflecting (green-blue) double-glazed glass. All the windows should extend from approximately four feet above the floor as far up as possible for maximum natural lighting. Where arc booths are to be placed against the wall, the window should not be built below the top of the arc booth.

AISLES

The aisles between the work stations should be 4 ft. wide for safe personnel traffic. The 4-ft. aisles permit students to maneuver weldments and equipment without danger of injury.

ELECTRICAL OUTLETS

Electrical outlets should provide power for the arc portable grinder, welders, spot welder and stationary grinder, and there should also be several excess outlets.

Electrical outlets should be provided at approximately 9-ft. intervals around the room with two each on any posts or supporting columns in the room. The attachment plugs should provide for a ground connection for all portable lights and equipment. The outlets should be amply wired for and wired to double-plug outlets. Polarized outlets are recommended to guarantee a ground connection.

The electrical area in these shops should have outlets for 220-v. three-phase alternating current and for 110–220 v., single-phase alternating current. The 3-phase outlets should be wired to provide at least 75 amp. per outlet, and should be protected with circuit breakers. These latter outlets are to be located at the arc-welding machine positions. The arc-welding machines should be located as close together as possible to reduce electrical installation costs. The 3-phase wiring should be standardized for direction of rotation.

All the 110-v. outlets should be not less than 4 ft. above the floor. These outlets should be wired for 110-v., 60-cycle, alternating-current, single-phase circuits. It is considered good practice not to exceed four outlets per fuse or circuit breaker. All those outlets should be polarized to guarantee a positive ground. One of these outlets should be provided for the spot welder. Three-phase, 220-v. current should also be wired into the welding shops for the air compressor, ventilating fans, power shears, grinders, motorized cutting equipment and any other equipment which requires motors that develop over $^1/_2$ hp.

VENTILATION (STATION)

An under-floor system of ventilation should be provided to carry away the fumes from the welding stations. The underground system is then connected to openings in the floor to which are connected fixed duct openings and also flexible conduits for the removal of localized fumes.

A forced convection fan of ample capacity should be provided to produce a reduced pressure in the underground duct at all times. The fan should have an automatic control with a visual indicator. The under-floor duct should not be less than 8 in. I.D. for each station. Those exhaust systems should remove 100 cfm. for each gas station and up to 1000 cfm. for each arc station.

The ducts should have clean-out facilities and traps to catch metal particles.

For buildings being converted and for those situations where underground vents are impractical, an overhead exhaust may be used. The main stack should extend above the highest roof point and those parts near the welding stations should be insulated (asbestos) to prevent holes being burned in them. Individual exhaust fans may be used in the smaller installations.

VENTILATION (ROOM)

Ventilation of the welding shop should be provided by using forced convection of fresh air to replace the exhaust-system air removal. The fan and motor capacity should be sufficient to provide a complete change of air each 10 min. minimum. The fresh air should equal the exhaust capacity. The ventilation for these rooms should be completely separate from the ventilation system of the others parts of the building unless self-closing louvers are used to prevent the backing of welding-shop air into the main vent ducts.

The American Society of Heating and Ventilating Engineers' recommendations should be followed. It is exceedingly important to provide adequate ventilation without causing objectionable drafts. Research has indicated that ozone generators do not take the place of adequate ventilation.

LIGHTING

The direct lighting system should provide for a minimum illumination of 30 foot-candles at 30 in. above the floor level for all three types. The hot-cathode-type lighting is preferred. It is recommended that smaller light sources of greater distribution (greater number) be used (approximately 100 watts for each welding station). Fluorescent lighting is acceptable. If incandescent bulbs are used, a diffusing glass globe is recommended, and the bulbs should be shielded with recommended local-electrical-code safety shields.

The lights should be 8 ft. or more above the floor, with special provisions provided for lighting around benches, shears, spot welders, grinders, etc. Spotlights may be used to light the individual welding station to provide enough illumination to eliminate lifting the goggles for weld setups.

WATER

Sufficient water should be piped to all of the three shops to provide metal-cooling facilities, resistance-

welder-cooling facilities and student washing facilities. The student washstands require both hot and cold water. The piping should be under the floor, if possible, but it may be run along a wall at least 12 ft. overhead if the under-floor piping is not permitted or is impractical. A drinking fountain should be provided within the shop. This drinking fountain can be located near the quenching basin. If hard water is used, it must be treated to prevent precipitation in the resistance-welder water passages.

SEWERS

A sewer system must be provided for the quenching basin, the student washing facilities, the spot welder, the drinking fountain, and for the floor drains. A trap should be installed in the sewer to capture the iron oxides (scale). This trap should be readily accessible.

ARTIFICIAL OR NATURAL GAS

If heating gas is available, all of the welding shops should be provided with this utility in ample capacity. The use of gas as a heating fuel simplifies the heating problem to the extent of eliminating storage facilities for cylinder stored fuel. Also natural or artificial gas may be used for heating soldering coppers, for torch soldering, for pilot lights, and for preheating metal structures. All the gas facilities must conform to the local code.

HEATING AND COOLING

Hot-water radiant heating in the floor slab is recommended for approximately 40% of the heating load.

Forced convection steam or hot-water heaters suspended from the ceiling may be used for 80% of the heat load. However, ceiling radiation panels are recommended for the heat load (80%).

The heat blast should be spread uniformly. Cooling may be obtained by using a flat roof with a water layer and by using the space-heater blowers. The tempering coils of the fresh-air inlet should have sufficient capacity to heat the make-up air to room temperature.

ALLOCATION OF WORKING SPACE

The space in the one-room welding shop should be well balanced to provide a good training program for the student.

The space divisions suggested are as follows:

Name	*Per cent*	*2400-sq.-ft. shop*
Gas welding area	30	720
Arc welding area	30	720
Resistance welding area	10	240
Power tool area	12	280
Lecture space	10	240
Tool crib	8	200

To economize on floor space a balcony above the tool crib is used for lockers and wash basins if the shop has at least a 14-ft. high ceiling.

The location of these areas depends to a great extent on the linear dimensions of the shop. However, the electrical power devices should be adjacent to each other; the tool crib should be as centrally located as possible, and the complete shop must be arranged to permit constant visual inspection from practically any point in the shop. It is also economical to locate all the plumbing in one general area.

CLOCKS AND BELLS

A clearly visible electrically operated clock of size sufficient to be easily read from all parts of the shop should be installed.

An electrically operated bell, controlled from the tool crib, should be installed to provide means for giving the clean-up signal.

Means should also be provided for installing a regular school change-of-classes bell and a public-address-system speaker.

COMPRESSED AIR AND COMPRESSED-AIR OUTLETS

All of the shops should be provided with compressed air outlets at 60–150 psi. pressure. The quantity should be ample to operate spot welders, air hammers, air grinders, etc. It should be located as far above the floor as possible to provide more space and help keep the shop clean. The air lines should be adequately sloped and provided with draining facilities. An air outlet should be provided near the preheater to enable the use of forced air and gas flames.

TOOL CRIB

The tool crib should be arranged along a wall that does not have windows. The crib should be rectangular in shape, not less than 8 ft. wide and 12 ft. to 16 ft. long. The construction should be of steel and wire mesh. The open side should be enclosed with counter-height shelving. Metal screening should completely enclose the remainder of the tool crib. If the crib is provided with a substantial ceiling structure, storage facilities or locker facilities may be provided on the tool-crib roof. In order to save as much floor space as possible, some communities may wish to use the space over the tool crib for student lockers. A grating should be installed in the window (have spacing only large enough for a shield). The tool-crib entrance door should be beside or as close to the tool window as possible. A work bench station with air and electrical outlets should be provided to enable the tool-room boy to maintain tools. The tool crib must be well lighted (30-foot-candles at the tool boards, etc.). Adequate well-labeled shelving sturdily built for storing metal, gas filler rod, electrodes and flux is recommended.

INTERIOR TRIM

The interior walls should be of glazed light-colored brick with black mortar or some other smooth ceramic surface for a height of not less than 8 ft. The remainder of the structure should be of fire-resistant materials.

The shop and lecture area should have glass-enclosed display shelves and display boards. Corridor display cases should be installed and they should be accessible from the shop only.

Lockers should be provided for the storage of students' clothes. These lockers are best located in a locker room that has toilet and shower facilities.

Wash basins should be provided for student use. These wash basins should be located near the lecture area. The wash basins should have automatic water-shut-off valves (foot operated).

The lecture and planning space should be separated from the shop by a dustproof (sealed) double-glass partition to keep as much welding dust and noise out of the lecture and planning area as possible. The glass partition will permit better supervision of the facilities.

MANIFOLDS

It is generally believed that it is more economical to use gas manifolds than to use individual cylinders for the gas stations. The manifolds save space, they reduce demurrage, and they are considered safer. One disadvantage of manifolds is the loss of the opportunity to train students in the use of complete stations with cylinders. This handicap can be eliminated by using one complete portable station and rotating all the students on this station.

It is preferred that the manifolds be of sufficient capacity and that they be constructed in compliance with the recommendations of the AMERICAN WELDING SOCIETY, the International Acetylene Association and the local building code.

QUENCHING BASIN

All of the welding-room layouts need to be equipped with a sturdily built quenching basin. This basin should have a scale trap that may be easily cleaned and with a wire basket to enable the easy removal of metal inadvertently left in the basin. It should have running water with an overflow provision. The drain from the spot welder could be piped to this basin.

PILOT LIGHTS

The most popular method of lighting gas torches is the spark lighter, that is, it is the most popular in industry and most schools will probably choose to emulate industrial practice. However, a safe economical method to light-gas station torches is to use a pilot light system. This system, with the natural or artificial gas flames mounted overhead makes the student ignite the torch high over the head level of the room. One American Gas Association approved lighter may be used for each two stations.

ARC WELDING BOOTH

The arc booths may be made of several materials. Steel partitions with a door hung on spring hinges is good practice. Steel booths with a wool-duck front curtain is used frequently. Booths made of a pressed synthetic board (Transite, etc.) with a self-closing door are probably the best. The material is self-insulating and is structurally strong. The arc-welding machines may be located inside the booth, just outside the booth or racked in a row opposite the aisle from the booths. These locations are listed in their order of preference. The interior of the booth should have an ultraviolet ray absorbent surface (special blue has been found satisfactory). Use air filters located on the machine when it is installed in the booth.

SUMMARY

There are many special features concerning a school welding shop that make it necessary to do some very thoughtful planning. If the shops are carefully designed, they will operate more efficiently and they will be much safer. The recommendations in this report are intended to serve as reminders to the planners of these shops and are not to be construed as requirements. A well-designed school welding shop can be obtained only when representatives from the welding industry, the architects, the school administrators and the welding teachers cooperate in its planning.

ACKNOWLEDGMENT

The speaker wishes to thank Harry Saul, Welding Instructor at Wilbur Wright Vocational High School, for his invaluable aid in the formulation of these tentative standards. The speaker also wishes to thank Ray Stitt, R. C. Mahon Co., and T. Crawford and A. E. Lindsey for their invaluable suggestions.

(*Continued from page 28*)

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How to Do Hard Facing

by P. C. Peck

HARD facing is slightly different from oxyacetylene fusion welding. First of all, in hard facing, the rod is not mixed with the base metal. It bonds with the surface only and becomes the new surface. This means that you melt only the surface of the base metal. Then the hard-facing rod is melted and spread, not mixed, over the surface.

This article tells how to hard face a corn planter shoe (see Fig. 1). However, you can follow the same instructions for hard facing runners, bucket teeth, road scraper blades, shafts, shear blades and hundreds of other parts that wear out rapidly in normal use.

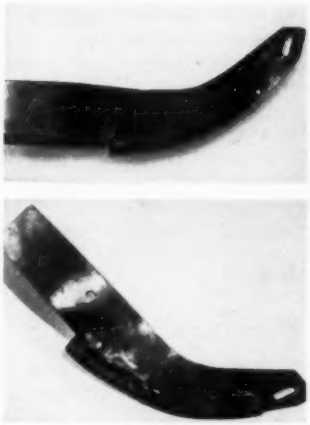


Fig. 1 (Top) Corn planter shoe before hard facing. (Bottom) Cutting edge of corn planter shoe after depositing rod

GETTING READY

The first step is to clean thoroughly the area of the corn planter show that will be hard faced. Use a power grinder and remove all dirt, rust and scale from along the cutting edge and about 1 in. back from the edge. Then select a good hard-facing rod, such as Haynes 93 alloy. Your distributor carries it along with several other rods which will do a good job.

PREHEAT CAREFULLY

After the part is cleaned, set it up in a vise or on a bench. Then attach a medium-size welding tip to the blow pipe and adjust it for a 3X acetylene flame, so the excess acetylene feather is three times as long as the inner cone. Begin heating the part slowly. Hold the blowpipe so that the flame makes an angle of 30 to 60 degrees with the side to be surfaced. Move the flame along the shoe until the metal appears to sweat or glisten. Keep the flame about $^3/_4$ in. in from the edge. The edge will get enough heat even though the flame does not contact it directly.

BEGIN HARD FACING

When the metal starts to sweat, bring the rod into the flame. Be sure that the base metal is not melted. Figure 2 shows how to produce "sweating." The metal under the flame becomes watery only, and is not melted.

When a puddle forms, it will flow down to the cutting edge of the shoe. As soon as the puddle starts to flow, lift the rod away, and direct the flame into the puddle. The puddle will spread over the edge. As the puddle spreads, bring the end of the rod into the flame again, and touch it to the front edge of the puddle. More rod will melt into the spreading puddle.

BUILD UP THE EDGE

Apply a smooth, thin deposit along the edge about $^1/_{16}$ in. thick. Move the rod forward in small circles. Keep it at the front edge of the puddle. Melt it a little at a time, and spread the puddle. Continue adding the rod and spreading the puddle until the entire surface is covered.

Try to build up the $^1/_{16}$-in. layer of rod in one operation, as shown in Fig. 3. You can move the flame back to fill in low spots, or smooth out high spots behind the puddle, but do this quickly. Be sure that the front edge of the puddle does not harden. And be sure that you melt only the top surface of the hard-facing de-

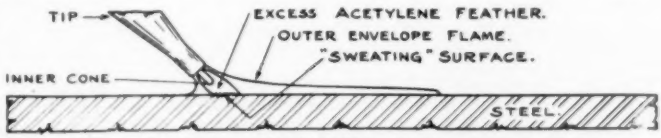


Fig. 2 Here is how to hold the blowpipe to produce sweating

P. C. Peck, The Linde Air Products Co., a Division of Union Carbide and Carbon Corp., New Orleans, La.

Fig. 3 Try to finish the hard facing in one operation. Allow to cool slowly

posit. Do not melt it down to the base metal.

When the entire edge is covered, remove the flame. Then check the deposit. If there are any cracks or small holes in the hard-facing material, heat the metal around the crack or hole until it becomes dull red. Then carefully play the flame into the hole and float out any specks of dirt that might be there. Let the hole close in, and add rod if necessary.

COOL THE PART SLOWLY

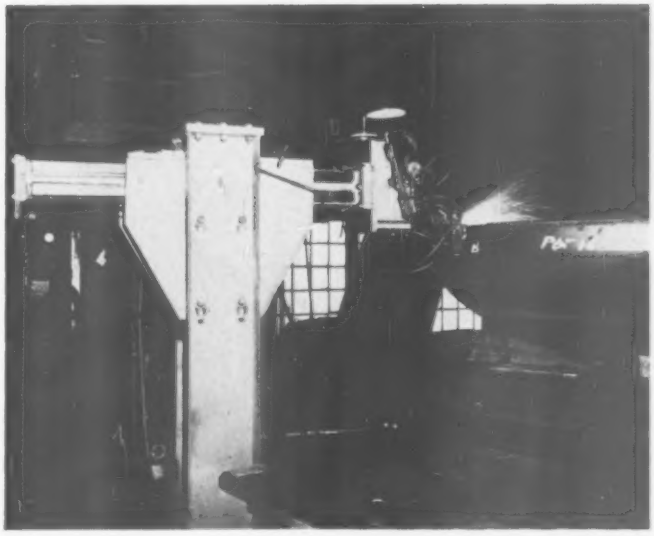
After you finish the job, let the shoe cool in air. If the deposit is fairly smooth and even, the shoe can be used without any finishing. If it is rough or has high spots, grind them down so you have a smooth, even deposit.

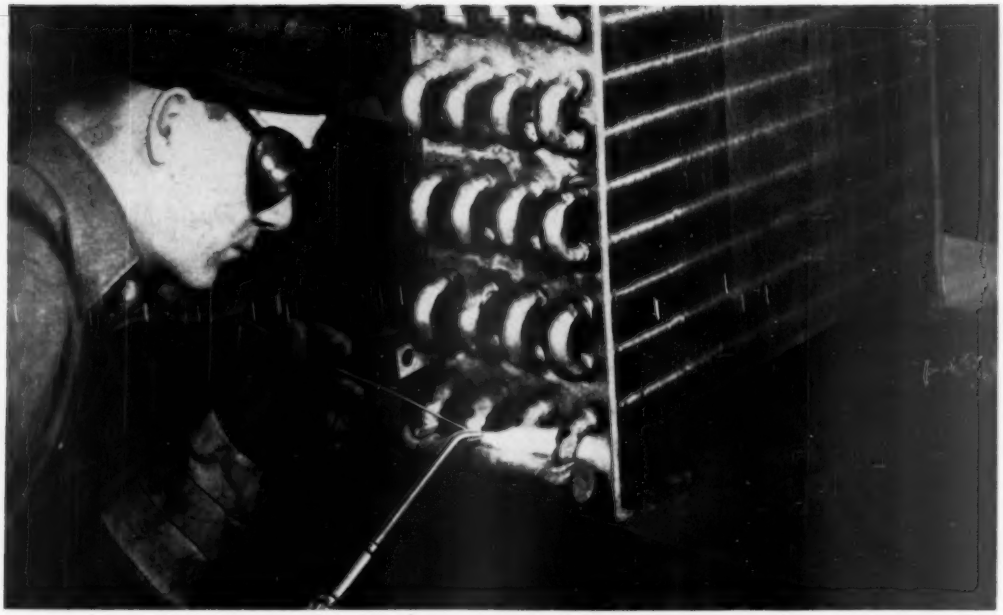
THINGS TO REMEMBER

1. Be sure the surface to be hard faced is thoroughly clean.
2. Use an excess acetylene flame.
3. Heat the base metal only until it sweats. Do not melt it.
4. Hold the blowpipe so that the tip of the inner cone of the flame is about $\frac{1}{8}$ in. from the surface of the base metal, with the rod almost touching the tip.
5. Spread the puddle with the flame, not with the rod.
6. Try to build up the deposit in one operation.
7. Allow the part to cool slowly, away from drafts.

12-Ft. Diameter Tank Heads Beveled with Oxyacetylene Plate-Edge Preparation Unit

TRIMMING the edges of this 12-ft. diameter tank head was accomplished with an Oxweld CM-37 plate-edge preparation unit with three adjustable nozzles. These dished heads are of $\frac{3}{4}$-in. steel and will become part of a tank car at the Sharon, Pa., plant of General-American Transportation Corp. The adjustable nozzle block is mounted adjacent to the tool support member of a large stationary boring mill. The horizontal arm is counterweighted to float back and forth to follow the contour of the head which is mounted on a turntable. The air-cooled riding wheel presses against the edge of the head and keeps the nozzles in correct position to the metal being cut. The combination of nozzles permits these heads to be simultaneously finished with a bevel and nose in one revolution of the turntable. Cutting speed is about 14 in. per minute.





Hints for Silver-Brazing of Stainless Steel

by J. P. Emmerich

STAINLESS steel is used today for vats, tanks and kettles in the canning or processing of food; on automobiles; on dairy equipment; and for much household equipment, including kitchen pans and utensils. Like other equipment, stainless steel parts must be repaired from time to time and it can be profitable business for the shop that is prepared to do the repair work. Shops also can make to order special equipment needed by farmers, milk companies and food processing companies.

Select the Proper Rod

Silver-brazing is an excellent way to repair or fabricate stainless steel equipment. By following some basic principles and with a few hours' practice, an operator can obtain silver-brazed joints that are strong and corrosion-resistant.

In repairing or fabricating stainless steel it is important to select the proper type of silver-brazing alloy because these alloys have different flow temperatures. An alloy must be used that will flow below the scaling temperature of the stainless steel. Table 1 indicates the groups of stainless steels and their scaling temperatures. Table 2 contains information on silver-brazing alloys—their composition and flow temperatures. By checking both tables the correct alloy can be selected. Sometimes, when a part is to be repaired, the type of stainless steel may not be known. However, usually a satisfactory job can be done with an alloy that flows at 1145 or 1175° F.

J. P. Emmerich is connected with Linde Air Products Co., a Division of Union Carbide and Carbon Corp. Newark N. J.

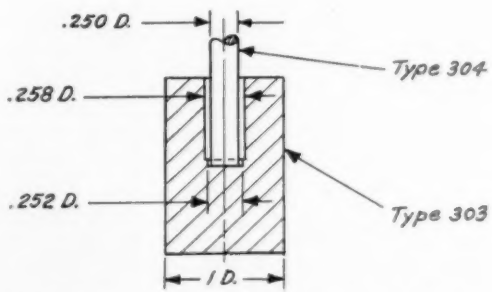


Fig. 1 The parts are prepared so that there is 0.003-in. clearance between the tube and the socket

Some Important Hints

In silver-brazing stainless steel, the first point is joint clearance. Proper joint clearance—0.003 to 0.004 in. on one side or 0.006 to 0.008 in. on a diameter—must be maintained on all types of joints. If clearance is too small the silver alloy will not flow completely into the joint. If the clearance is too great, too much silver

Fig. 2 Emery cloth is used to abrasion-clean the surfaces of both parts immediately before fluxing and silver-brazing

Fig. 3 Parts are placed in a suitable penetrating cleaner to remove the last traces of oil, grease or other materials which may still be on the surfaces

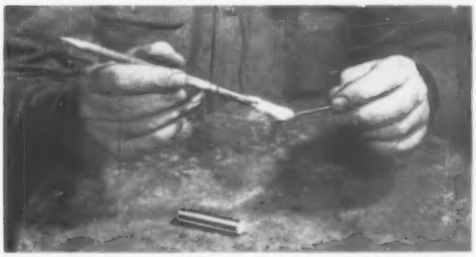


Fig. 4 Flux is applied generously to all surfaces to be silver-brazed. It is also applied to adjacent surfaces to prevent oxidation around the joint. The flux should be a paste

Fig. 5 With an excess acetylene flame start to heat the parts. Hold the tip of the flame 1 to 1½ in. away. Gradually move toward the parts until the flame is about ¾ in. away

Fig. 6 When the parts turn a dull red, touch the rod to the joint every few seconds. The alloy will flow as soon as the proper heat is reached

Fig. 7 Draw back the flame slowly to maintain uniform heating. Allow the parts to cool in the air and then quench in water. This will remove any remaining flux

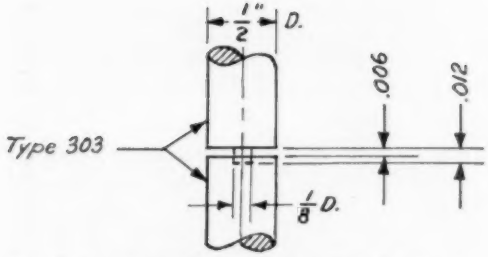


Fig. 8 In making a butt joint, clearance is again important. On this part a recess was made in one face and a lug on the other face to get proper line-up

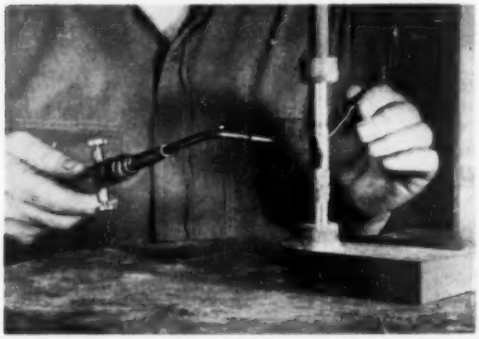


Fig. 9 When you start to heat hold the flame 1 to 1½ in. away. Then move up to ⅜ in., apply the silver alloy and back away gradually

Table 1

	Group A		Group B		Group C	
Grain structure →	*Martensitic*		*Ferritic*		*Austenitic*	
Composition →	*Straight chromium*		*Straight chromium*		*Chromium and nickel*	
Heat treating →	*Hardenable*		*Nonhardenable*		*Nonhardenable*	
	Type	Scaling temp., °F.	Type	Scaling temp., °F.	Type	Scaling temp., °F.
	403	1200	329	1600	302	1600
	410	1250	406	1300	303	1600
	414	1200	430	1300	304	1600
	416	1250	430-F	1300	305	1600
	420	Corrosion	443	1600	309	2000
	420-F	resistance	446	1900	310	2000
	440-A	impaired			316	1650
	440-B	when heated			317	1600
	440-C	to over			321	1600
	440-F	700°F.			347	1600

In Group A, types 403, 410, 414 and 416 can be silver-brazed, but since the scaling temperature is extremely low, only the lowest flow point silver alloy can be used. The silver alloy flow temperature should not exceed 1175° F. for good results. In order to silver-braze these types after hardening, it is necessary to silver-braze under 1200° F., otherwise the heat of silver-brazing will anneal the section heated. Types 420, 420-F, 440-A, 440-B, 440-C and 440-F are only used in the hardened state and since any reheating over 700° F. detracts from their corrosive resistance they are not normally silver-brazed.

Group B stainless steels are well suited for silver-brazing. However, extreme care must be exercised in heating types 406, 430 and 430-F because of their low scaling temperature. It is best to use a low melting point silver alloy that flows below 1175° F. Types 329, 443 and 446 have higher scaling temperatures and a larger choice of silver-brazing alloys is available. Silver alloys with a flow temperature of 1145 or 1175° F. or a silver alloy containing 2 or 3% nickel, flow temperature 1275° F., may be used with equally good results.

Group C, the 18-8 group, since they have relatively high scaling temperatures, can be silver-brazed with any of the listed silver-brazing alloys with good results.

alloy will flow into the joint and it will be weak.

The second important point to remember is the method of heating the part. A 3× or 4× excess acetylene flame is best. This means that the acetylene feather should be three or four times the length of the inner cone.

Fig. 10 This picture shows the butt joint before brazing and after the brazed part had been bent in a vise. Scale and flux have been removed

Fig. 11 For lap joints, the facing sides and the edges should be cleaned with emery cloth, dipped into the cleaner and painted with flux

Fig. 12 Top and bottom sheets must be heated to get a uniform heat. Touch the heated area often with the silver alloy. It will flow at the right temperature

Table 2—Silver-Brazing Alloys

Composition, %				Flow Point, ° F.	Color	Recommended use (see Table 1) for group listings
Silver	Copper	Zinc	Cadmium			
50*	15.5	15.5	16	1270	Yellow	Group B, C
50	15.5	16.5	18	1175	Yellow	Group A, B, C
47	20	17.5	15.5	1240	Yellow	Group B, C
45	15	16	24	1145	Yellow	Group A, B, C
35	26	21	18	1295	Yellow	Group C and some group B
34	27	18	21	1300	Yellow	Group C and some group B

* Contains 2-3% nickel.

The third important point is the proper sequence of operation. The following steps must be followed for silver-brazing stainless steel parts of any type:

1. Remove excess grease, oil or dirt from the part.
2. Degrease and abrasion-clean the part immediately before fluxing and silver-brazing.
3. Place the part in a penetrating cleaner to remove all traces of oil and grease.
4. Apply flux generously.
5. Heat with an excess acetylene flame.
6. Apply silver alloy to the joint.
7. Saturate the joint with heat without increasing the temperature of the part.
8. Cool the part in air until the silver alloy has solidified, then quench in water.
9. Remove the scale and any remaining flux.

The pictures and sketches show three typical joints and how they were made.

Four at One Blow

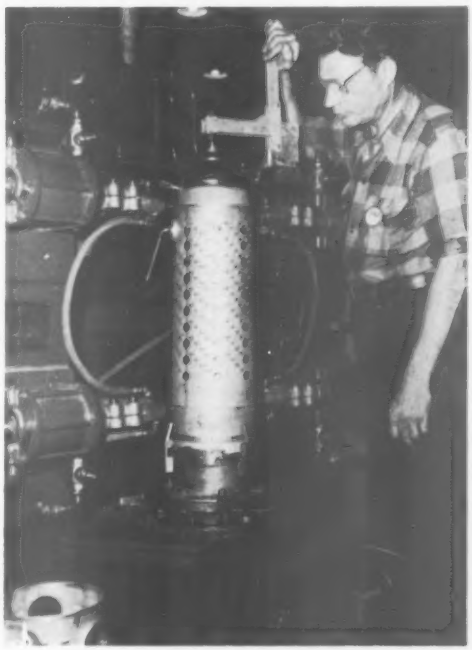
ONE of the most specialized welding machines ever devised has recently been installed at Ryan Aeronautical Co. for use in production of jet engine components. Designed by the Thomson Welding Machine Co., this new metal "stitcher" is tailored to perform a unique operation on a single type of Ryan jet component. It makes four simultaneous spot welds.

Described as a "post spot-tacking machine," this unusual tool serves as both an assembly and fabrication machine in the manufacture of inner combustion chambers for the General Electric J-47 jet engine. It quickly aligns the three major parts of the combustion chamber and spot tacks them together prior to seam welding.

Most striking feature of the machine is a brass and copper tool, or arbor, which serves double duty as an alignment tool and a central electrode. At the top and bottom of the arbor there are expanding sections consisting of four pie-shaped segments. The movement of a tapered pin, actuated by an air cylinder, expands these segments into the assembly. Four horizontal electrodes move simultaneously against the expanded sections to discharge their welding current.

In operation, the combustion chamber shell, exit shell and cap are placed on the arbor. With a touch of a foot switch, the expanding sections move outward, bringing the assembly into exact alignment and concentricity. Another foot control causes the electrodes to move in and fire, making four spot tacks at each impulse. The assembly is rotated automatically, so that a total of 32 spot tacks can be accomplished in eight swift rotations.

The new machine saves time and effort and eliminates the need for an extra employee. Previously, the assembly of this structure required two employees: one to assemble the parts in a jig and another to hold this unwieldy jig in a spot welding machine while single spot welds were made. Because of the necessary weight



of the jig, this operation was fatigue-producing. Then the assembly had to be removed from the jig.

The Thomson post spot-tacker eliminates this handling and effort by aligning the parts, spot tacking them and releasing them in one setup which is relatively effortless. One machine operator can assemble the combustion chambers five times as fast as two employees could formerly accomplish the work. Because of the satisfactory performance of the new equipment and the increasing orders of G-E combustion chambers, Ryan is acquiring another machine of this unusual design.

Photo and data, courtesy the Ryan Aeronautical Co., San Diego, Calif.

the AMERICAN WELDING SOCIETY

activities • related events

Final 1950–51 Board of Directors Meeting

The final (sixth) meeting of the Board of Directors of the AMERICAN WELDING SOCIETY 1950–51 year was held in Parlor H, 5th floor, Book-Cadillac Hotel, Detroit, Mich., on Thursday, Oct. 18, 1951, at 3:00 P.M., with the following in attendance:

Members: H. W. Pierce, *Chairman*, C. H. Jennings, F. L. Plummer, R. S. Donald, L. C. Bibber, J. J. Chyle, T. J. Crawford, A. F. Davis, O. B. J. Fraser, J. Grodrian, La Motte Grover, H. O. Hill, H. L. Ingram, Jr., T. B. Jefferson, A. J. Moses, H. E. Rockefeller, G. N. Sieger, H. N. Simms, J. R. Stitt, C. B. Voldrich and A. E. Wisler.

Staff: J. G. Magrath, *Secretary*, and F. J. Mooney, *Assistant Secretary*.

Guests: R. S. Green, Director-at-Large, 1951–52; I. A. Oehler, Director-at-Large, 1951–52; E. R. Seabloom, 2nd Vice President, 1951–52; and M. S. Shane, Director-at-Large, 1951–52.

Absentee Members: C. A. Adams, W. F. Boyle, J. H. Humberstone, G. S. Schaller, L. C. Stiles and H. S. Swan.

President's Report

The Chairman advised that the address he presented, as President, at the Annual Business Meeting preceding this Board meeting, constituted his report for the year of his term of office as President and, accordingly, he was submitting same to the Board for their acceptance and, he hoped, their approval.

Action: Upon motion, duly seconded, the President's Report was accepted and approved.

Secretary's Report

The Chairman advised that copies of the Secretary's Annual Report had been distributed to each Board Member at this meeting, a summarization had been orally delivered by the Secretary and the Annual Business Meeting, and the report was now presented for acceptance and approval.

Action: Upon motion, duly seconded, the Secretary's Report was accepted and approved.

Manufacturers Committee

The present text of the by-law provision for this Committee's activities is as follows:

A Manufacturers' Committee composed of representatives of Manufacturers of Welding Equipment and Supplies, which Committee shall cooperate with the Convention Committee in making arrangements for exhibitions and shall handle matters of general interest to the Manufacturers."

The foregoing sometimes has been interpreted as confining this Committee to activities and matters of general interest to the Manufacturers in connection with exhibitions only. It is desired that a broad interpretation be given so that the Committee may, within its rights, handle matters of general interest to the Manufacturers other than those relating to exhibitions.

Action: Upon motion, duly seconded, the Board of Directors approved the following ruling, such to be incorporated in the Rulings of the Board of Directors:

"The Board of Directors interprets the wording and intent of the by-law providing the scope of the Manufacturers Committee as giving the Manufacturers Committee the right to advise and consult the Board of Directors of the SOCIETY on any matters affecting the interest of the Manufacturers of Welding Equipment and Supplies."

New Standing Committee

President Pierce proposed that a new Standing Committee be created, its membership to consist of executive-management representatives from industry interested in welding, particularly users; that this Committee be of the nature of a ways and means, as well as an industry advisory group, and that its membership be in the order of appointee-designation by A.W.S. Further, that this Committee be developed in scope and manpower, for the purpose of performing activities essential to the welfare and the strengthening of A.W.S. economically and industry-wise.

The conference group of the Executive Committee studied and deliberated upon President Pierce's recommendation. It was recognized that heretofore industry users at the executive level have not, to an extent believed essential by the SOCIETY, taken active interest in the work of the SOCIETY. The conference group recommends to the Board of Directors that such Committee be created.

Action: Since a representative number

The Board in Session

of industry users at executive level have not taken an active interest in the work of A.W.S., upon motion, duly seconded, the formation of a Committee of industry users at executive level was approved; the activity of that Committee to include the determining of ways and means for obtaining the objectives that would enlist their support.

Recommendation: It was recommended that the incoming Board of Directors, 1951-52 year, consider this action of the outgoing Board, 1950-51 year, and that this Board's action be referred to the incoming Board for its immediate consideration and further action.

Fund Raising Committee

O. B. J. Fraser, Chairman of the Fund Raising Committee, verbally reported his Committee's progress on the fund raising program. He stated that his Committee was now in agreement upon the proposed plan, after having studied other plans. The plan formulated comprises the preparation of an economical folder or brochure bespeaking the work, accomplishments, desires and requirements of the A.W.S. Technical Activities and the preparation of a suitable letter designed for interesting potential subscribers in contributing to funds for supplementing the operation of Technical Activities; this letter to be of a suggestive pattern only and to be signed by business executives now interested in A.W.S., to other business executives in organizations of activities similar to theirs, urging all other executives in their particular industry to support the SOCIETY'S Technical Activities in a manner similar to that support given by the writer.

Mr. Bibber observed that he hoped that this activity would in no way conflict with the fund-raising activities of the Welding Research Council, which activities likewise benefit A.W.S. It was felt by those attending that there would be no conflict.

Action: Upon motion, duly seconded, the report of the Chairman of the Fund Raising Committee was accepted and approved.

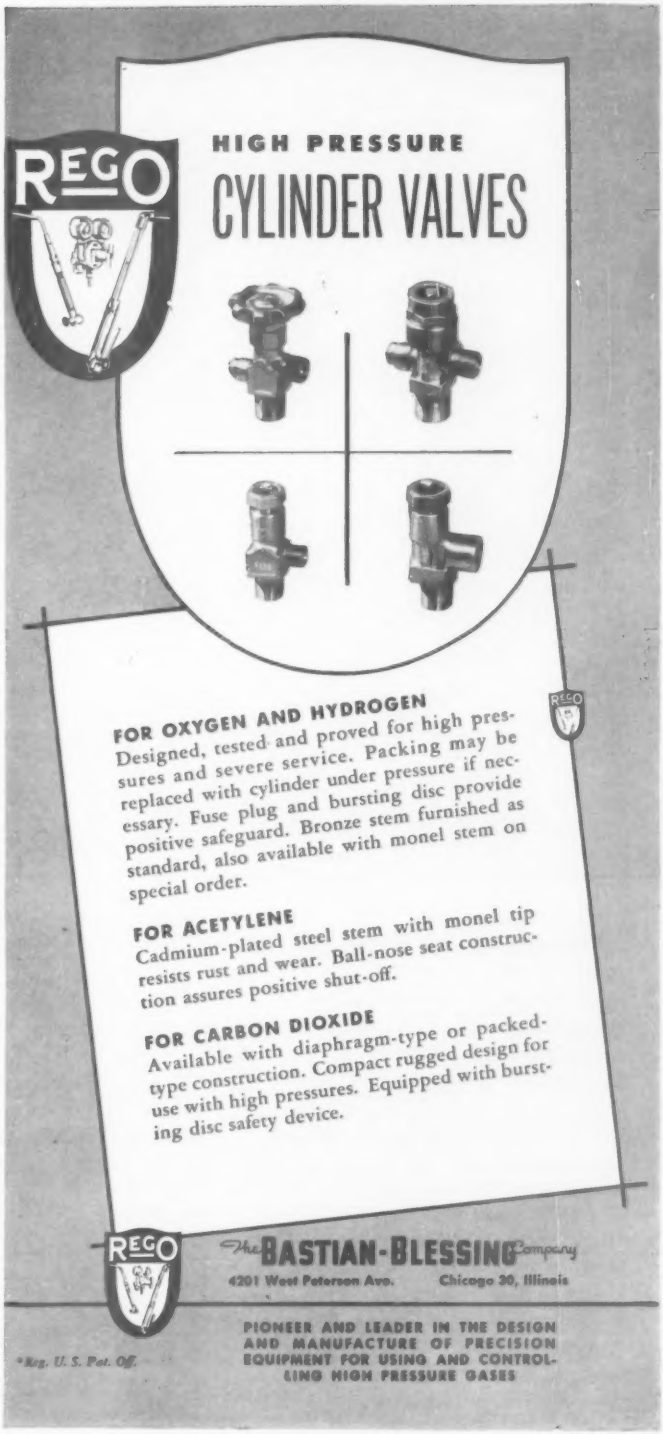
Membership Classification Committee

The conference group of the Executive Committee recommends to the Board of Directors the appointment of I. Morrison, Chairman of the National Membership Committee, to membership on the Special Committee on Membership Classification. This recommendation is based upon the obvious interest of Mr. Morrison in this Special Committee's deliberations.

Action: Upon motion, duly seconded, Mr. Morrison's appointment to the Special Committee on Membership Classification was approved.

Omission

The authors, R. S. Zeno and H. L. C. Leslie, of the paper, "The Inert-Arc Welding of Thin-Walled Tubing," published in the November 1951 issue of THE WELDING JOURNAL, inadvertently omitted in the "Acknowledgment" the name of J. P. Frandsen of the Knolls Atomic Power Laboratory, General Electric Co.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Designed, tested and proved for high pressures and severe service. Packing may be replaced with cylinder under pressure if necessary. Fuse plug and bursting disc provide positive safeguard. Bronze stem furnished as standard, also available with monel stem on special order.
FOR ACETYLENE
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Board of Directors Meeting

The first meeting of the Board of Directors of the AMERICAN WELDING SOCIETY for the 1951–52 Year was held in Parlor H, 5th floor of the Book-Cadillac Hotel, Detroit, Mich., on Thursday, Oct. 18, 1951, at 3:51 P.M., with the following in attendance:

Members: C. H. Jennings, *Chairman*, F. L. Plummer, E. R. Seabloom, L. C. Bibber, T. J. Crawford, A. F. Davis, R. S. Donald, O. B. J. Fraser, R. S. Green, La Motte Grover, H. L. Ingram, Jr., T. B. Jefferson, A. J. Moses, I. A. Oehler, H. W. Pierce, H. E. Rockefeller, M. S. Shane, G. N. Sieger, J. R. Stitt, C. B. Voldrich and A. E. Wisler.

Staff: J. G. Magrath, *Secretary*, and F. J. Mooney, *Assistant Secretary*.

Guests: J. J. Chyle, Director-at-Large, 1950–51; H. O. Hill, Director-at-Large, 1950–51.

Absentee Members were: C. A. Adams, E. N. Adams, J. U. Durham, A. P. Maradudin, G. S. Schaller and E. O. Williams.

Appreciations

(*a*) Chairman C. H. Jennings voiced his sincere appreciation to outgoing President H. W. Pierce and to the Board of Directors for their activities and accomplishments during the 1950–51 administrative year. He expressed the hope that he would, during his term of office as President, with the help of the Officers and Board Members, be able to carry on the excellent work started by, and equal to the work accomplished by, his predecessor, Mr. Pierce. He stated he would devote his full efforts to that task.

(*b*) Chairman Jennings called for a motion authorizing Past-President Fraser and Secretary Magrath to draft a letter from the Board of Directors to retiring President Pierce, expressing the appreciation of the SOCIETY for his services during the 1950–51 year as President and Chairman of the Board of Directors.

Action: Upon motion, duly seconded, the foregoing recommendation was approved.

(*c*) Chairman Jennings called upon retiring President Pierce and, on behalf of the Board of Directors, presented him with a silver rimmed inscribed gavel and gavel block in case, such being a symbolic expression of the appreciation of the members of the Board for his services during his year of activity as President of the SOCIETY and Chairman of the Board of Directors.

Staff Officer Appointments

Upon motion, duly seconded, recommendation was approved that J. G. Magrath serve as the SOCIETY's Secretary for the 1951–52 administrative year.

Upon motion, duly seconded, recommendation was approved that F. J. Mooney serve as the SOCIETY's Assistant Secretary and Assistant Treasurer for the 1951–52 administrative year.

Documental Signature Authorization

Upon motion, duly seconded, the Chairman's recommendation that the Treasurer and the President, and in the absence of either, the Assistant Treasurer, be authorized to sign checks and other documents in behalf of the SOCIETY, was approved.

National Program Committee

The Chairman called attention again to the agenda item covering the list of proposed appointees to the Standing Committees and, in particular, Committee 5, the National Program Committee. He observed that this was based upon the recommendations of the Special Committee for Determining the Scope, Purposes and Requirements of the A.W.S. Annual Meeting, which recommendation included the reorganizing of the Program Committee to an organization which could serve the entire National Program, including welding conferences, such as at Ohio State University, Purdue, special program activities on the West Coast during the Western Metal Congress, the Centennial of Engineering, 1952, in Chicago, and other regional meetings, as well as the Annual Meeting of our SOCIETY

Action: Upon motion, duly seconded, the reorganized pattern of the National Program Committee was approved as submitted.

Executive Committee Authority

The Secretary called attention to the action of the 1950–1951 Board of Directors extending special authority to the Executive Committee for the 1950–51 year, in the order following:

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P. R. Mallory & Co., Inc.	3
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Metal & Thermit Corporation	2
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Tweco Products Company	Inside front cover
United States Testing Company Inc.	90
Union Carbide and Carbon Corporation Linde Air Products Company	4
Victor Equipment Company	6
Wagner Manufacturing Co.	74
Waterman Products Co., Inc.	74
Westinghouse Electric Corporation	72
J. H. Williams & Co.	90

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Scrappy says, "Aid defense — more scrap today . . . more steel tomorrow."

Engineered to reduce pressure loss

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• Looks simple, doesn't it . . . that welding fitting? Not so! It's *engineered* . . . for shape, for size, for dimensions, for wall thickness . . . all so that it will do its share of the piping job for you . . . without trouble and at top efficiency. True circularity, smooth inner walls, and full radius combine to reduce pressure loss of any flow of fluid. Micrometer-checked uniform wall thickness and forged-in strength combine to assure long-life, dependable service.

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Welded piping reduces maintenance for City within a City

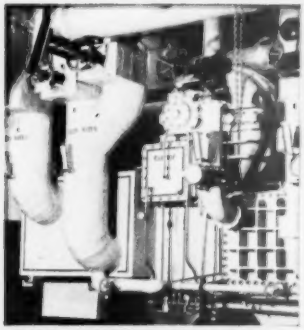
● Service requirements for the Rockefeller Center group of 15 skyscrapers add up to big figures.

The Center consumes nearly half a billion pounds of steam each year.

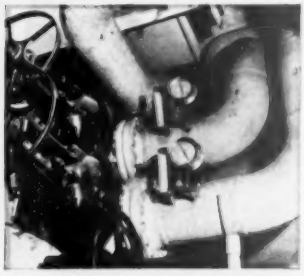
The famous outdoor skating rink is kept in use even when temperatures are in the high sixties.

A total of 8,000 tons of refrigeration is available for air conditioning.

But maintenance on all the miles of piping is kept to a minimum. All critical piping systems are welded, and directional changes made with TUBE-TURN Welding Fittings. Thus piping is permanent and leakproof, and vital services are maintained without interruption.



Piping for refrigeration machines, ranging up to 14 inches, is trouble-free. Use of TUBE-TURN Welding Fittings also speeded up erection of piping, since most assemblies were shop-welded, then tied into the systems they serve. Insulation was applied easily and is permanent.



District heating plant supplies steam at 150 psi. Steam is then sent through headers fabricated with TUBE-TURN Welding Fittings ranging up to 12 inches, and distributed via steam stations. Optimum flow conditions have been obtained and maintenance overhead minimized.

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An Executive Committee consisting of not less than five members of the Board of Directors shall be empowered to act for the Board of Directors in the administration of the affairs of the SOCIETY as may be necessary, from time to time, between meetings of the Board of Directors. Action taken by the Executive Committee shall be subject to approval by the Board of Directors at their next regular meeting."

The Chairman recommended that comparable authority be extended to the 1951–52 Executive Committee.

Action: Upon motion, duly seconded, it was approved that the same special authority be extended to the Executive Committee for the 1951–52 year.

Next and Future Meetings

The Chairman called for recommendations regarding locations and approximate dates for future meetings of the Board of Directors during the 1951–52 fiscal year. Discussion followed. As a result of the deliberation, it was decided that the next meeting of the Board would be held in January, subsequent to January 15th such to be preferably in Washington, D. C., but if accommodations were difficult to obtain, then it could be arranged for holding in Baltimore, Md. It was recommended that the third meeting of the Board of Directors be held in March, in Indianapolis, Ind., in conjunction with the annual meeting of I.A.A. It was decided that it was necessary to hold the late August meeting of the Board in New York City for the purpose of reviewing the operating budget for the 1952–53 fiscal year. The fifth and final meeting of the Board would naturally be held in October in Philadelphia, the site of the Annual Meeting of the SOCIETY.

Auditor's Report

DAVID JOSEPH & COMPANY
Accountants—Tax Consultants
40 East 49th Street
New York 17, N. Y.

October 9, 1951

AMERICAN WELDING SOCIETY
33 West 39th Street
New York, N. Y.

Sirs:

We present herewith the financial report on the status of the SOCIETY as at August 31, 1951, and the results of operations for the fiscal year then ended.

The results of operations for the year show an excess of income over expenses in the amount of $4997.18. This result is very gratifying inasmuch as the past few years have resulted in operating losses.

Income from Membership Dues increased by $2080 over the preceding year. Income from this source, before allocation of Dues Income to JOURNAL Subscriptions, amounted to $118,128 as against $116,048 for the preceding year. The registered membership increased from 7132 to 7374.

JOURNAL Advertising revenue increased from $47,734 to $57,618; $4,550 of this increase resulted from additional paid lineage and the balance resulted from an increase in rates which was put into effect at the end of 1950.

The comparison of budget against actual operations shows that the total income for the year was $6997.59 above the budget estimate. Of this amount, $5586.55 resulted from increased subscriptions and sales of JOURNALS and Reprints.

We herewith certify that the Statement of Assets and Liabilities and Comparative Statement of Resources submitted herewith fairly present the financial status of the SOCIETY as at August 31, 1951, and that the Statement of Income and Expense correctly sets forth the results of operations for the year then ended.

Very truly yours,

(Signed) DAVID JOSEPH
DAVID JOSEPH & COMPANY

American Welding Society Statement of Income and Expenses for the Fiscal Year Ended August 31, 1951

INCOME:		
Membership Dues	$118,128.15	
Less: Amount Allocated to Subscriptions	16,428.93	
		$101,699.22
Dues Income Allocated to JOURNAL:		
Subscriptions	16,428.93	
JOURNAL Advertising	57,618.88	
JOURNAL Supplements	4,822.46	
JOURNAL Cuts	2,715.83	
Subscriptions and Sales of JOURNALS	23,086.55	
Annual Meeting	8,060.81	
Codes, Standards and Binders	6,772.67	
Welding Handbook	20,514.15	
Reprints of Handbook Chapters	217.20	
Welding Metallurgy Book	3,115.76	
A.W.S. Educational Books	412.69	
Other Educational Books	1,833.05	
Emblems and Certificates	738.15	
Interest Income	241.24	
TOTAL INCOME		$248,277.59
EXPENSES:		
JOURNAL Text Matter and Advertising	$ 98,447.94	
Technical Activity and Printed Matter	19,256.70	
Membership and Education	22,451.11	
Welding Handbook	14,234.29	
A.W.S. Educational & Welding Metallurgy Books	7,913.81	
Section Activities	9,401.67	
Annual Meeting	17,082.89	
Administration	12,131.88	
Section Refunds	27,462.03	
Emblems and Certificates	338.22	
Other Educational Books	1,445.94	
Purchase of Equipment	661.31	
Adams Lectureship Award	199.30	
Publicity	774.87	
Yearbook and Membership Directory	3,201.61	
Employees Insurance Expense	5,794.06	
1% of Gross Income to Reserve Funds Account	2,482.78	
TOTAL EXPENSES		243,280.41
Excess of Income Over Expense		$ 4,997.18

New

New WD-4200 and smaller WD-4150 are compact, portable welders with wide current range and versatility of operation. 30 volts, 50% duty cycle—220/440 volts; 60 cycles; 150 and 200 amps.

D-C WELDER

Features Versatility and Ease of Operation, High Production, Low Costs

VERSATILE

The new G-E WD-4200 can be used with:

- a variety of electrodes from $\frac{1}{16}$ to $\frac{5}{16}$ inches, and as large as $\frac{1}{4}$ in. for special short jobs
- wide current range—from 30 to 250 amps, or as low as 15 amps with a special, easily-installed resistor
- both Inert-Arc and metal-arc welding

EASY TO OPERATE

Operators like the new manual starter, simple dial controls and a stable arc which can be maintained at any current setting. The compact, lightweight WD-4200 is easily portable, too.

INCREASES PRODUCTION

High instantaneous voltage makes the arc strike every time on heavy or light-gage stock, reducing spoilage.

The WD-4200 is suited to both fast metal-arc welding and Inert-Arc welding of copper, stainless steel and alloy steels, including sheet metal from $\frac{1}{32}$ to $\frac{1}{16}$ inches.

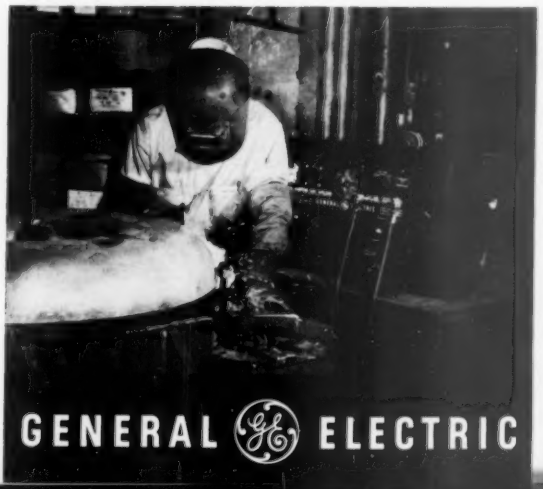
LOWERS COSTS

Reduces Power Costs 10 to 15%

The WD-4200 substantially lowers costs because the full range of current is obtained without the use of a current-limiting resistor.

Limited current peaks conserve electrodes and help prevent spatter and burn-through. You save on original cost, production *and* upkeep expense!

It's easy to select *just the right current* (above photo.) The operator can use a wide variety of electrodes for either Inert-Arc or metal-arc welding.



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Please send me your bulletin, GEC-847, which describes the newest G-E WD4200 Generator Welder.

Bulletin needed for:

..... Reference purposes
..... Planning immediate project

Name .....
(Please print)
Position .....
Address .....
City ..... Zone ..... State .....

American Welding Society Statement of Assets and Liabilities August 31, 1951

Current Assets			
Cash in Banks	$37,985.57		
Cash on Hand	150.00	$38,135.57	
Accounts Receivable:			
Journal Advertising	$ 684.90		
Supplements, Cuts and Journals	616.59	1,301.49	
Bulletins, Codes, Emblems and Handbooks	4,756.31		
Total Current Assets			$44,193.37
Current Liabilities			
Accounts Payable	$10,001.08		
Refunds Due Sections on Paid Dues	922.26		
Payable to Reserve Funds Committee Account—1% of Gross Income	1,181.24		
Membership Dues Received in Advance of Membership Year	6,075.00		
Receipts Held in Suspense	719.25		
Pay Roll Taxes Withheld	56.70		
New York City Sales Tax Payable	9.82	$18,965.35	
Reserve for Resistance Welder Manufacturers Association Awards		2,250.00	
Reserve for Program of Testing Metallizing Applications for Corrosion Resistance		1,417.98	
Reserve for A. F. Davis Undergraduate Welding Award Program and Travel Fund		3,283.19	
Reserve for Lincoln Gold Medal Award		1,649.11	
Total Current Liabilities			27,565.61
Net Working Capital			$16,627.76
Add: Other Assets			15,686.66
			$32,314.42
Less: Deferred Income			5,977.79
Net Worth—August 31, 1951			$26,336.63

American Welding Society Statement of Resources August 31, 1951

Net Worth as at August 31, 1951		$ 26,336.63
Miller Memorial Fund Account:		
Cash on Deposit in Union Dime Savings Bank		519.10
Reserve Funds Committee Account:		
Cash on Deposit in Emigrant Industrial Savings Bank	$7,484.46	
Due from Society	1,181.24	
$1500 par U. S. War Savings Bonds, Series F	*1,330.50	
$7000 par U. S. Treasury Bonds, 2½%, due 1963–68	7,000.00	
$10,000 par U. S. Treasury Bonds, 2½%, due 12/15/69–64	10,000.00	
$8000 par U. S. Treasury Bonds, 2½%, due 12/15/62–59	*8,007.32	
$2000 par U. S. Treasury Bonds, 2½%, due 12/15/62–59	*2,053.01	
$15,000 par U. S. Treasury Bonds, 2½%, due 1965–70	15,000.00	
$10,000 par U. S. Treasury Bonds, 2½%, due 1965–70	10,000.00	
$7500 par U. S. Treasury Bonds, 2½%, due 3/15/71–66	7,500.00	
$10,000 par U. S. Treasury Bonds, 2½%, due 8/15/72–67	10,000.00	
$7500 par U. S. Treasury Bonds, 2½% due 12/15/67–72	7,500.00	
$6000 par U. S. Treasury Bonds, 2½%, due 12/15/67–72	*6,157.28	
$7000 par U. S. Treasury Bonds, 2½%, due 12/15/67–72	*7,158.75	
$8000 par U. S. Treasury Bonds, 2½%, due 12/15/67–72	*8,181.94	
$2000 par U. S. Treasury Bonds, 2½%, due 12/15/62–59	*2,059.02	110,613.52
Total Combined Resources		$137,469.25

* Purchase Premiums and Discounts Amortized to August 31, 1951

$2250 in Prizes for Papers on Resistance Welding: *Design—Application—Research*

Contest Closes July 31, 1951

Prizes and Contest Rules

Announcement has been made of cash prizes to be awarded in 1952 by the Resistance Welder Manufacturers' Association for outstanding papers dealing with resistance welding subjects. The total amount of the awards is $2250, and a wide choice in subject matter is allowed in order to assure eligibility to all papers which cover worth while and significant achievements in the field. The contest judges will be appointed by the American Welding Society, and awards will be made at the 1952 Fall meeting of the Society. The prizes and rules governing the contest are as follows:

One prize of $750 for the best paper emanating from an industrial source, consulting engineer, private or government laboratory, or the like, the subject matter of which is concerned specifically with resistance welding. There are no restrictions on the scope of the subject matter. For example, it may be devoted to redesign of a product or products for resistance welding, improvement (from a welding and cost viewpoint) in a present design for resistance welding, resistance welding research, development of new procedures to broaden the field of application of resistance welding, etc. Papers in these categories should explain the economic importance of the accomplishments described—that is, cost savings, production improvement, scope of application, etc.

A prize of $500 for the second best paper in the above classification. A prize of $250 for the third best paper in the above classification.

A prize of $300 for the paper emanating from a University source (the author of which is either an instructor, graduate student or research fellow) which in the opinion of the Board of Award is the greatest original contribution to the advancement and use of resistance welding from this source.

A prize of $200 for the second best paper in the above classification.

A prize of $250 for the paper emanating from a University source (the author of which is an under-graduate student) which in the opinion of the Board of Award is the greatest contribution to the advancement of resistance welding from this source.

While each prize award is the total for the paper, each individual author and co-author, if any, will be presented with a suitable certificate.

The contest is open to anyone, without restriction, from the United States, its possessions and Canada. It is also open to any member of the American Welding Society in any grade from any place in the world. The contest is considered as having opened Aug. 1, 1951. In order to

It's true! Acetogen offers a better, cheaper, faster and complete STEEL FABRICATION SERVICE

better—

because we employ the amazing Acetogen Process to flame-cut your steel. This is the process you've heard and read about—the new way of flame-cutting steel to ±1/16" tolerance. Acetogen cut plates are *precise*. They have "feather" edges and a sufficiently low Brinell to allow machining. They are ready for welding without any further preparation.

cheaper—

because we flame-cut—not grind—your work to exact tolerances. With the revolutionary Acetogen Process, it is no longer necessary to grind even the toughest bevel or radius! We save—and pass on to you—the cost of elaborate grinding machines and the men to operate them.

faster—

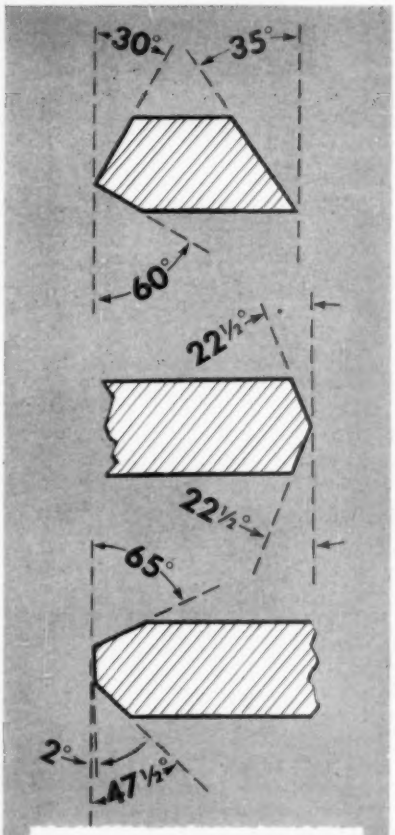
because Acetogen-cutting is infinitely quicker than laborious grinding. In one department, with one plate positioning, with one operator, in one stroke—we make the *finished* cut to the required tolerance. After cutting, the plate is immediately ready for forming, assembly, or other operations.

and complete—

because the brand-new Acetogen plant at Chester, Pa. contains all the tools and equipment necessary to do any fabricating job—including flame-cutting, welding, forming, sub-assembling—on armor plate, low-carbon and high-alloy steels, as well as on all other types of metals. It is operated by men thoroughly experienced in ordnance and aircraft metal-working.

Let us quote on your defense work . . .

Our organization has full security clearance to contract for defense work through the Department of the Army, Philadelphia Ordnance Division. Send us drawings, sketches, or blueprints for any fabricated work that you have, including the most difficult. We will promptly quote price- and time-schedules that will amaze you. Get those drawings *now* and write today! Acetogen Fabricators, Inc., Room 822 Commercial Trust Bldg., Philadelphia 2, Pa.



What do you think of these bevels? They are all found on one piece of armor plate that we are now fabricating in quantity. Other fabricators would have to *grind* these edges to required tolerances. Our *flame-cutting* achieves the same exact tolerances—and we pass the savings in time and money on to the customer!

ACETOGEN FABRICATORS, INC.

Originators of precision flame-cutting

be eligible for this contest all papers must be forwarded so as to be delivered at AMERICAN WELDING SOCIETY headquarters no later than 5:00 P.M., July 31, 1952. Papers which are to be presented at the Annual Meeting of the AMERICAN WELDING SOCIETY in October may also be entered in this contest, in which case a draft or copy of the paper must be filed with the AMERICAN WELDING SOCIETY not later than July 31, 1952. To be eligible, author must indicate paper has not been considered previously in the contest, or shall not have been presented or published prior to its appearance in the WELDING JOURNAL, except in a school paper in the case of a paper from a University source.

All papers submitted in the contest become the joint property of the R.W.M.A. and the AMERICAN WELDING SOCIETY, who will retain all rights thereto. The AMERICAN WELDING SOCIETY will appoint five judges, who will judge the relative merits of the various papers submitted and make the awards accordingly. The decision of the judges will be final.

For the author or authors to be eligible for this award, the paper shall describe clearly original work done by them or under their supervision on resistance welding in any of its aspects by any method or process. The paper shall be a full disclosure of the subject. The paper shall contain no statement which is unethical advertising or sales promotion. The paper may contain statements of fact, including the names of either individuals or organizations of any kind, commercial designations, trade names, etc. The minimum length requirement is 2500 words.

All papers must be typewritten, double spacing, written on one side only of blank white paper. Photographs, charts, graphs, etc., may either be attached directly to the copy or may be detached, in which case they should be clearly identified with figure numbers, captions, etc.

Papers entered in this contest should be sent to AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N.Y. If mailed to arrive not later than July 1, 1952, three copies should be furnished. If mailed to arrive between July 1 and July 31, 1952, six copies should be furnished.

Employment Service Bulletin

Positions Vacant

V-262. Chief Inspector—Ordnance Division. Age: 35–45. Salary: $700–$750 per month (no incentive). College degree (M.E. preferred) or equivalent. Two years' supervisory experience either production or inspection. Should know welding techniques intimately and be familiar with statistical quality control. Previous experience preferably with progressive companies producing heavy standardized equipment. Must be firm, decisive, yet fair and honest.

V-263. Production Superintendent—Ordnance Division. Age: 37–45. Salary: $750 per month plus $250 per month incentive. College degree (M.E. preferred) or its equivalent. Five years' experience as production executive, preferably in companies producing heavy equipment using modern welding techniques. Well informed and have successful record in production management. Good administrator with ability to get results in terms of quality, meeting delivery schedules at budgeted costs and with good labor relations. Resourceful and a constructive planner and doer. Strong personality, firm and decisive, yet fair and honest. Able to obtain desired performance by suppliers. Good record of accomplishment in developing and maintaining sound industrial relations and high employee morale.

Services Available

A-623. Welding Engineer: Age 37, married, three children. Thirteen years diversified metal fabricating experience covering all phases of welding, brazing and soldering. Practical experience in production, processing, quality control, metallurgy, research and engineering development work. Capable of coordinating projects from the initial research and development phases on through all steps of manufacturing to the final product. Familiar with forging and casting practices, material standards and specifications including estimating, vender and customer contact work. Will consider engineering, metallurgical, quality control or production work. Prefer West Coast or Rocky Mountain area.

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113	263	400	950	1500
125	275	450	1000	1550
138	288	500	1050	1600
150	300	550	1100	1650
163	313	600	1150	1700
175	325	650	1200	1750
188	338	700	1250	1800
200	350	750	1300	1850
213	363	800	1350	1900
225	375	850	1400	1950
238	388	900	1450	2000

$2 each

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- TEMPERING
- FORGING
- CASTING
- MOLDING
- DRAWING
- STRAIGHTENING
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Report on Membership Status for the Year Ending August 31, 1951

Membership Status for Year Ending August 31, 1951

	A	B	C	D	E	F	Total
Total Membership, Sept. 1, 1950	111	3019	3801	193	8	..	7132
Gains (new members, increases in classification and reinstatements)	15	603	678	91	1	6	1394
	126	3622	4479	284	9	6	8526
Losses (resignations, cancellations due to delinquency and decreases in classification)	11	430	660	51	..	..	1152
Total Membership, August 31, 1951	115	3192	3819	233	9	6	7374
Net changes during year	4	173	18	40	1	6	242

Membership Classifications: A—Sustaining Members; B—Members; C—Associate Members; D—Student Members; E—Honorary Members; F—Life Members.

Society Membership August 31, 1951

Section	A	B	C	D	E	F	Total
District 1, New York and New England							
Boston	3	72	89	1	..	..	165
Bridgeport	2	19	32	1	..	..	54
Hartford	1	51	35	..	..	..	87
Niagara Frontier	..	84	100	..	..	..	184
Northern New York	1	11	52	9	2	..	75
Rochester	2	21	79	..	..	..	102
Syracuse	..	21	86	2	..	..	109
Western Massachusetts	..	22	9	..	..	..	31
Worcester	1	35	30	..	..	..	66
District 2, Middle Eastern							
Lehigh Valley	2	34	64	1	..	1	102
Maryland	..	43	130	..	..	..	173
New Jersey	3	162	133	..	1	..	299
New York	13	198	195	3	3	3	415
Philadelphia	9	141	160	..	1	..	311
Richmond	..	20	37	..	..	..	57
Roanoke	1	4	11	..	..	..	16
Susquehanna Valley	..	15	21	..	..	..	36
Washington	1	32	56	..	..	..	89
York-Central Pa.	3	29	27	..	..	..	59
District 3, Southern							
Atlanta	..	17	7	..	..	..	24
Birmingham	..	23	41	..	..	..	64
Chattanooga	1	8	29	..	..	..	38
Nashville	..	6	2	1	..	..	9
New Orleans	2	8	10	..	1	..	21
Pascagoula	..	2	16	..	..	..	18
District 4, Central							
Cincinnati	..	28	52	1	..	..	81
Cleveland	6	155	251	1	..	..	413
Columbus	3	35	54	34	..	..	126
Dayton	..	32	48	20	..	..	100
Detroit	11	308	211	11	..	1	542
Mahoning Valley	5	39	31	..	..	..	75
Northwestern Pa.	1	20	43	2	..	..	66
Pittsburgh	7	114	102	2	..	..	225
Saginaw Valley	..	29	43	24	..	..	96
Toledo	..	9	18	2	..	..	29
Western Michigan	..	37	47	1	..	..	85
District 5, Mid-Western							
Anthony Wayne	..	41	11	..	..	..	52
Chicago	10	158	188	1	1	..	358
Eastern Illinois	..	18	8	..	..	..	26
Indiana	..	55	33	1	..	..	89
Louisville	..	21	26	..	..	..	47
Michiana	..	9	45	..	..	..	54
Milwaukee	3	66	113	1	..	1	184
Northwest	9	56	76	1	..	..	142
Peoria	..	18	55	..	..	..	73
Tri-State	..	5	15	..	..	..	20
District 6, Mid-Southern							
Colorado	1	33	22	..	..	..	56
Dallas	..	36	39	11	..	..	86
Houston	..	78	105	..	..	..	183
Kansas City	..	37	39	..	..	..	76
Oklahoma City	..	15	41	20	..	..	76
St. Louis	..	49	49	1	..	..	99
Tulsa	..	50	9	1	..	..	60
Wichita	1	19	40	34	..	..	94
District 7, Western							
Arizona	..	21	25	..	..	..	46
Inland Empire	..	6	4	..	..	..	10
Long Beach	2	10	52	..	..	..	64
Los Angeles	4	92	157	7	..	..	260
Portland	..	26	14	..	..	..	40
Puget Sound	..	31	45	..	..	..	76
Salt Lake City	..	41	8	22	..	..	71
San Francisco	1	67	66	1	..	..	135
Not in Sections							
Domestic	2	65	68	16	..	..	151
Foreign	4	185	115	..	..	..	304

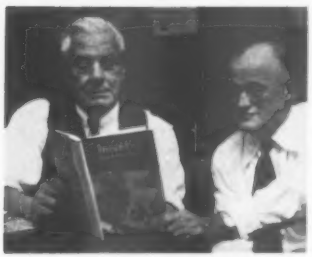
Record of Membership

Membership Year	A	B	C	D	E	F	Total
1919	62	116	13	25	1	..	217
1920	83	35	25	97	1	..	341
1921	81	192	45	254	1	..	574
1922	50	186	81	277	1	..	595
1923	50	193	121	317	3	..	684
1924	51	203	153	299	3	..	709
1925	51	237	219	303	3	..	813
1926	49	273	258	262	3	..	845
1927	51	310	262	226	3	2	854
1928	47	343	299	210	3	11	913
1929	47	378	360	207	3	21	1016
1930	44	498	476	279	3	43	1343
1931	40	443	615	273	3	50	1424
1932	33	403	639	257	2	40	1374
1933	21	350	613	261	2	37	1284
1934	20	345	692	273	2	43	1375
1935	24	348	892	300	2	48	1614
1936	29	362	956	367	2	50	1766
1937	76	547	1175	536	1	43	2378
1938	88	987	1586	786	1	90	3538
1939	87	1146	1563	860	2	35	3693
1940	83	1230	1826	1165	2	38	4344
1941	78	1288	2215	1009	2	45	4637
1942	82	1458	2598	1036	2	50	5226
1943	105	2103	3276	1124	2	28	6638
1944	130	2831	4185	470	2	18	7636
1945	142	3146	4699	131	2	19	8139
1946	131	2979	4224	56	5	16	7411
1947	139	3048	4169	37	7	..	7400
1948	130	3071	4114	69	9	..	7393
1949	116	3051	3889	126	9	..	7191
1950	111	3019	3801	193	8	..	7132
1951	115	3192	3819	233	9	6	7374

NEW LITERATURE

Incentive Management

Incentive Management by James F. Lincoln is a philosophy through which workers and managers in business and industry can develop themselves as skillful individuals and work together cooperatively to everyone's benefit rather than work at cross purposes to everyone's expense. Large 6- x 9-in. page size, 288 pages, bound in gold embossed cloth covered board, price $1.00 in U. S. A., $1.50 elsewhere, postage prepaid. Publisher: The Lincoln Electric Co., Cleveland 17, Ohio.



"Jim" Lincoln and "Charlie" Davis review the first copy of "Incentive Management"

Metco News

The current issue of the *Metco News* (Vol. 5, No. 11) describes and illustrates several money-saving metallizing applications. Copies of this publication may be obtained by writing to Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N.Y.

Test Data on Plastic-Coated Lenses and All-Plastic Lenses

The Thermacote Co. announces the results of an exhaustive test conducted on plastic-coated lenses versus all-plastic lenses, the test being conducted by the California Testing Laboratories of Los Angeles.

Under simulated welding conditions, the test has data and photographs showing the comparison of the two types of lenses under various welding conditions.

Copies of this test, complete with photographs, are available and may be obtained by writing to the Thermacote Co. at 320 Jefferson St., Newark, N.J., or at 1005 S. Santa Fe Ave., Los Angeles 21, Calif.

Hard Surfacing Welding Electrodes

Tips on using Seaco AC/DC Hard Surfacing Welding Electrodes to get the most out of maintenance are outlined in the *Manganal Marketer* (Vol. 2, No. 10), available on request from the Stulz-Sickles Co., 134–142 Lafayette St., Newark, N.J.

This publication describes the welding procedure and practical application of Seaco in protecting and increasing the life of steel parts subject to impact and abrasion.

Gas Welding and Cutting Apparatus

Victor Equipment Co., 844 Folsom St., San Francisco 7, Calif., announces the publication of their new apparatus catalog 20-C. The purpose of this catalog is to present complete information on Victor apparatus through carefully planned illustrations and charts to make selection and ordering as easy as possible. Your copy of this four-color apparatus catalog may be obtained by writing directly to the company at the above address.

Identification of Metals and Alloys

A new booklet, *Rapid Identification of Some Metals and Alloys*, is now available, without charge, through the Technical Service Section of the Development and Research Division of The International Nickel Co., Inc., 67 Wall St., New York 5, N. Y. This booklet covers, in outline form, simple spot-testing procedures for metals and alloys which can be used in the laboratory and in the shop.

New Aircomatic Catalog

A new illustrated catalog on Aircomatic equipment and wires has been announced by Air Reduction Sales Co., a Division of Air Reduction Co., Inc.

This catalog describes the Aircomatic Gun for manual operation, the Aircomatic Head for automatic operation and the Aircomatic Wires for use with either manual or automatic equipment. Photographs and sketches of the equipment are supplemented by on-the-job illustrations of a number of applications.

Copies of this new Aircomatic Catalog (Form ADC 717) may be obtained by writing to the nearest Airco office or to Air Reduction, 60 E. 42nd St., New York 17, N.Y.

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Send for this free booklet of latest information on Resistance Welding.
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Welding, Brazing and Cutting Alloys

An illustrated folder, 6 pp., 3¹/₄ x 6¹/₄ in., on welding, brazing and cutting steel with torch and arc has just been released by All-State Welding Alloys Co., Inc., White Plains, N. Y. Contained are complete instructions for use, techniques of application and description of the properties of twelve alloys for welding and brazing and one alloy for cutting, particularly developed for use on all types of steel. Copies of this folder are available on request at White Plains. Copies also may be obtained from distributors throughout the country handling All-State Alloys and Fluxes.

Welding Design Manual

The availability of the 4th ed. of *Manual of Welding Design and Engineering* has been announced by Eutectic Welding Alloys Corp.

Enlarged to 72 pages, the new edition contains 62 new photographs and 132 new drawings together with data and how-to-do-it articles; illustrated with application drawings; weld diagrams; tables containing information on melting temperatures, tensile strengths, corrosion factors, charts, etc.

This manual gives detailed information on over 100 different "Eutectic Low Temperature Welding Alloys" for use on cast iron, steel, copper, brass, bronze, aluminum, die cast, stainless, nickel, Monel, magnesium, etc., and covers all heating methods: torch, arc, furnace, inert arc, induction, etc.

The 72-page book is available without charge upon request from Eutectic Welding Alloys Corp., Dept. "P," 172nd St. and Northern Blvd., Flushing 58, N.Y.

Stud Welding Design Manual

"How to Design for Nelwelding," a concise 8-page manual prepared to aid designers and production engineers in the fast, economical solution of fastening problems, has been issued by the Nelson Stud Welding Division, Gregory Industries, Inc., Lorain, Ohio.

In addition to listing stud specifications and recommended stud selection guides, the manual presents detailed suggestions concerning template design, provisions for accommodating fillet and a table on minimum stud clearances applicable to the portable Nelson stud welding gun.

Rod Selection Guide

A *Rod Selection Guide for "Amscoating" with Amsco Hardfacing Products* has just been released by American Manganese Steel Division of The American Brake Shoe Co. This chart breaks down into basic operations, the parts that can and should be hard faced for longer life. The fields are Agriculture, Materials Working, Mining and Excavating, Crushing and Pulverizing, Materials Handling, and Power Transmission. The *Rod Selection Guide* gives the name of the part, the recommended rod, sizes and method of application. Write for *Bulletin "SG"* to the American Manganese Steel Div. Chicago Heights, Ill.

Resistance Welding Data Book

P. R. Mallory & Co., Inc., of Indianapolis, announces that a completely revised and re-written *Resistance Welding Data Book* is now available. This Fourth Edition, containing photographs, charts, tables and diagrams of pertinent, up-to-date welding information, is the first new edition of the *Resistance Welding Data Book* to be published in seven years.

Chapter titles include: "Fundamentals of Resistance Welding," "Spot Welding," "Projection Welding," "Seam Welding," "Flash and Upset Butt Welding," "Butt-Seam Welding," "Metals to Be Welded," "Alloys to Be Welded," "Resistance Welding Electrodes, Dies, and Holders," "Recommended Practices for Resistance Welding," "Standard Methods for Testing," and "Resistance Welds."

Over 500 pages and 1000 illustrations are included in the new Data Book which has been prepared by Mallory engineers and metallurgists.

The price of the Data Book is $3.00. Copies are available at P. R. Mallory & Co., Inc., Welding Division, Indianapolis 6, Ind.

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A Division of Air Reduction Co., Inc.

Booklet on Design for Welding

A new 20-page illustrated booklet, "Improved Design for Welding," has been announced by Linde Air Products Co., a Division of Union Carbide and Carbon Corp. The booklet describes the economies of good welding design as compared to haphazard design methods. Included in the booklet are reprints of the papers, "Plan Not Little Things," by G. F. Nordenholt, Editor of *Product Engineering* and "Applications of Welded Design for Cost Reduction," by R. H. Bennewitz, Linde Air Products Co. A summary of standard welding symbols abstracted from the "American Welding Society Summary of Standard Welding Symbols" is also included.

This booklet may be obtained without charge from the nearest office of the Linde Air Products Co., or by writing direct to Linde Air Products Co., 30 E. 42nd St., New York 17, N. Y.

Chart on Welded Tubing

The scope of sizes of Electric Resistance Welded Carbon Steel Tubing produced by the Welded Tube Division of The Babcock & Wilcox Tube Co., Alliance, Ohio, has been broadened. As an aid to those using ERW tubing for either mechanical or pressure purposes the company has published a chart covering the range of sizes which they now produce. This chart is broken down to reflect both average wall and minimum wall requirements in 0.08/0.18% and 0.20/0.30% carbon steel grades produced to various ASTM, ASME, AAR and government specifications for pressure tubing and standard specifications for mechanical tubing. It covers a size range of $\frac{1}{2}$ to 4 in., inclusive, outside diameter and 0.032 to 0.213 in. average wall thickness or 0.030 to 0.203 in. minimum wall thickness.

Copies of this new chart are available without charge on request to the company's sales office in Alliance, Ohio.

Metallizing Handbook

A new metallizing handbook has recently been published by Metallizing Engineering Co., Inc., 38-14 30th St., Long Island City 1, N. Y. The 250-page text, a completely new work, is profusely illustrated with photographs, diagrams and charts. It provides a comprehensive coverage of the technical as well as practical aspects of the process. Various methods of surface preparation and the application of metallized coatings for machine element work, for corrosion work, and for many special production jobs, are thoroughly detailed along with valuable supplementary information.

This includes such miscellaneous applications as the repair of blowholes in casting, brazing and soldering, glass and ceramics, mass coatings in tumbling barrels, electric circuits and model work, etc.

Price of the cloth-bound edition has been set at $3.00 per copy, postpaid in the United States and Great Britain only.

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To assist you in your re-tooling problems . . . or to help you analyze your present welding methods so that you may enjoy maximum speed, efficiency, and economy in many of YOUR production problems . . .

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SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Stainless Steels

Baltimore, Md.—N. (Nick) C. Jessen, AWS, Babcock & Wilcox Co., Barberton, Ohio, gave a clear-cut, interesting, though highly technical, talk on "The Welding of Low-Alloy and Stainless Steels," at the Nov. 16, 1951, meeting of the AWS *Maryland* Section, held at the Engineers Club, this city. Miss Amy Winslow, Director of the Enoch Pratt Free Library, Baltimore, was the coffee speaker. Her talk on "Are You Getting the Most out of Your Public Library?" was very well received and she was questioned at some detail.

Prior to the dinner the "Preheating with Cocktails" period was held. Following the technical session, light refreshments were served. This period always brings forth a great deal of additional discussion with the speaker and with other members present.

Low-Alloy Steels

Bethlehem, Pa.—Sidney Low, AWS, Registered Professional Engineer, Chapman Valve Mfg. Co., Indian Orchard, Mass., was the guest speaker at the Dec. 3, 1951, meeting of the AWS *Lehigh Valley* Section held at the Hotel Bethlehem. Mr. Low's illustrated technical talk was on "The Welding of Low-Alloy Steels."

An educational talk by W. D. Price, Air Reduction Sales Co., and E. Benyo, Bethlehem Steel Co., on "Flame Cutting," was an after-dinner feature.

Symposium

Bridgeport, Conn.—R. A. Lalli, AWS, President of The R. A. Lalli Co., this city, and C. E. Lane, Sales Engineer of Electroloy Co., also this city, were the guest speakers at the steak dinner meeting of the AWS *Bridgeport* Section held on Thursday, Nov. 15, 1951, in Shelton, Conn. Mr. Lalli's subject was the "Welding of Jigs and Fixtures for the Aircraft Industry." Mr. Lane's subject was "Tooling for Resistance Welding."

Coffee speaker was Thomas Burkhard, Sales Mgr., Mitchell Dairy, Bridgeport, a subsidiary of The Borden Co. His subject, from down on the farm, a "Success Story of Elsie the Cow."

Harold Grosberg, AWS, Welding Supt., Farrel Birmingham Mfg. Co., gave a report on the 1951 Annual Meeting and Metal Show held in Detroit.

Production Fixtures

Buffalo, N. Y.—Cecil C. Peck, AWS, President of Cecil C. Peck Co., Cleveland, Ohio, described "The Use of Fixtures in Production of Weldments," to 81 AWS members and guests at the Nov. 29, 1951, meeting of the AWS *Niagara Frontier* Section held at the Sheraton Hotel, this city. A buffet supper was enjoyed by 69 AWS members and guests before the meeting.

Ferrous Welding Metallurgy

Chicago, Ill.—Dr. R. H. Aborn, AWS, Assistant Director of Research, U. S. Steel Co., Kearny, N. J., presented a very interesting, though highly technical, address on "Recent Aspects of the Metallurgy of Ferrous Welding," before the AWS *Chicago* Section. Eighty-two AWS members and guests saw an excellent high-speed film of arc behavior with several different classes of electrodes. For those interested in the value of such a film, it is highly recommended as it contains the most effective photography of arc behavior seen in a long time. The film and Dr. Aborn's delivery held the audience right up to the very end.

As an innovation and general interest activity there was shown a film on "Sport Flashes." All this activity was held in Burke's Grill & Restaurant on Friday, Nov. 16, 1951.

This Section has set up an interesting program for the year covering various aspects of welding. For example, on January 18th the featured talk will be on "Design of Welded Piping"; on February 18th "Inert Gas-Shielded Arc Consumable Electrode Welding"; March 21st "Resistance Welding"; April 18th "Brazing for High Temperature Service"; May 16th "Inspection of Weldments." All of these featured addresses will be delivered by prominent people in industry.

Quality Control

Cleveland, Ohio.—Paul Setzler, engineering consultant for the Baldwin-Lima Hamilton Corp., with welding engineering experience in responsible positions with Lukenweld and Lincoln Electric, discussed the subject "Quality Control in Weldment Manufacture" at the Wednesday, November 14th, dinner meeting of the AWS *Cleveland* Section held at the Allerton Hotel, this city. Mr. Setzler discussed the subject from three approaches: design, manufacture and inspection. While inspection was important, the speaker emphasized that design and procedure were of paramount importance. Mr. Setzler pointed out that good design is the first requirement of a good weldment. The next requirement is good shop practice. Finally, properly qualified operators who understand the procedure and whose performance is adequately supervised is the last requirement. Slides and examples were used to illustrate Mr. Setzler's talk, which was followed by a question period.

The coffee speaker on this occasion was Mike Shane, well-known Cleveland Section past-chairman and at present member of the Board of Directors. Mike's talk was titled "From the Ponderable to the Imponderable" and was an easily understood exposé of such imponderable phenomena as the preservation of the butterfly species in Leipsig, Germany, although the process was made difficult by the initial separation of the components by many kilometers. In order not to stray too far afield from welding interest, Mike indicated the importance of the elusive electron in arc welding and clearly explained the imponderable function of the electron in arc welding.

Resistance Welding Applications

Columbus, Ohio.—J. H. Cooper, AWS, Chief Sales Engineer, Taylor-Winfield Co., Warren, Ohio, addressed 55 members and guests at the November 9th dinner meeting of the AWS *Columbus*, Ohio Section, on the subject "Special Resistance Welding Applications." Mr. Cooper's extemporaneous, illustrated talk, though nontechnical, was of general interest to all. This meeting was arranged and conducted by the Student Branch of the AWS Columbus Section located at Ohio State University.

Metallurgy

Dallas, Tex.—Anton L. Schaeffler, AWS, Metallurgist, Welding Research, Allis-Chalmers Mfg Co., Milwaukee, Wis., was the speaker at the AWS *Dallas* Section dinner meeting held on Wednesday, Nov. 14, 1951. Mr. Schaeffler presented an illustrated talk on Metallurgy for Welders and Supervisors—Postheat and Preheat." Following his talk refreshments were served all around.

Jigs and Fixtures

Denver, Colo.—A. N. Kuger, AWS, Mechanical Engineer, Technical Sales Dept., Air Reduction Sales Co., New York, talked on "Jigs and Fixtures for Welding" at the Tuesday, Nov. 13, 1951, dinner meeting of the AWS *Colorado* Section, held at the Oxford Hotel, before 46 AWS members and guests. Mr. Kugler's talk was semitechnical and covered methods and applications. This is an interest-arousing subject. Mr. Kugler, Stevens Institute of Technology, 1925, is a licensed Professional Engineer in both New York and New Jersey and member of AWS, ASME, ASTE and AVA. He used slides for illustrating his talk. A film, "The Land of the Incas," was shown after the meeting.

Welded Design Elements

Detroit, Mich.—Charlie Gehringer, Manager of the Detroit Baseball Club, attracted one of the largest dinner groups with his talk on "Welding a Ball Club," highlighting the behind-the-scenes efforts in developing a winning team, at the AWS *Detroit* Section meeting held on Friday, Nov. 16, 1951.

The featured speaker at the technical meeting was Leon C. Bibber, AWS, Welding Engineer, U. S. Steel Co., Pittsburgh, whose address was entitled "Elements of Welded Design." Mr. Bibber considers the design as the most important single tenet in welding. He believes design failures to be more frequent than weld failures. Mr. Bibber pointed out that if design is proper, a weld would probably stand up with moderately poor welding, whereas if design is wrong the weld will be wrong in spite of the best welding. Frequent slides were used to show the various types of weld joints with examples of both good and bad design and to illustrate the futility of attempting to correct bad design by good welding.

Arc Welding Redesign

Erie, Pa.—Charles Lytton, AWS, and Charles Richardson, AWS, District Managers of The Lincoln Electric Co., were the speakers at the Tuesday, October 23rd, meeting of the AWS *Northwestern Pennsylvania* Section held at Soudan's Restaurant, this city. Their talk on "Redesign for Arc Welding" was accompanied by a film entitled "Designing Machinery for Arc Welding," both of which were well received.

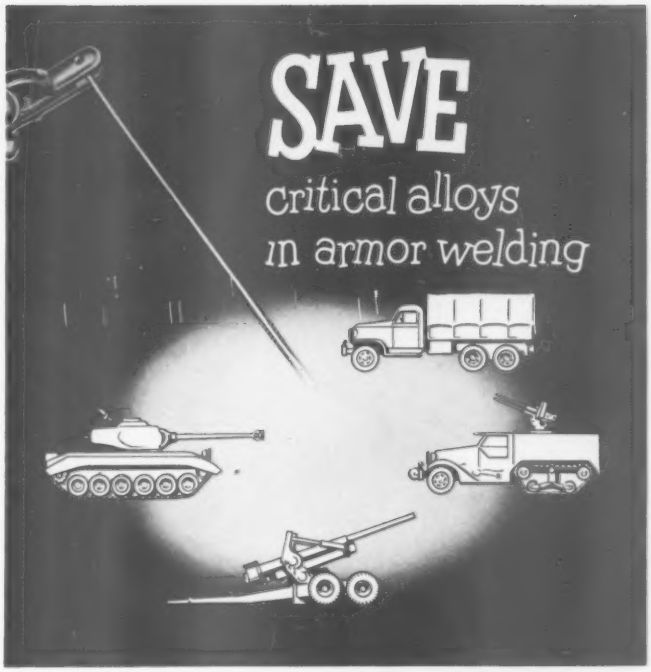
Maintenance Welding

Erie, Pa.—Lew Gilbert, AWS, Editor of *Industry and Welding*, talked on "Maintenance Welding" at the Wednesday, November 21st, meeting of the AWS *Northwestern Pennsylvania* Section held at the General Electric Community Center, this city. Mr. Gilbert gave a clear-cut, interesting talk on the subject and presented new facts and methods.

Following the technical session, a film and travel talk on the "International Boy Scout Jamboree in Austria" was presented by Fred T. Perry, Jr., AWS, of the General Electric Co. This was thoroughly enjoyed by all.

Better Designing

Ft. Wayne, Ind.—Leslie S. McPhee, AWS, Welding Supervisor of the Whiting Corp., Harvey, Ill., covered all phases of problems encountered in engineering and designing welded heavy structural work to small weldments and subassemblies, at the Friday, Nov. 16, 1951, meeting of the AWS *Anthony Wayne* Section held at Indiana Technical College. He stressed the importance of better designing of welding products to make easier welding and to lower costs. A great number of slides were shown through the courtesy of the Whiting Corp. on heavy structural work in traveling cranes and the success of keeping down



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"buckles" and warpage in these parts. The AWS film, "Applied Welding Engineering," was shown and well received.

Resistance Welding

Grand Rapids, Mich.—Jack Ogden, AWS, Fisher Body Division, General Motors Corp., Detroit, Mich., in a not too technical talk brought out some very useful points on resistance welding, at the Nov. 26, 1951, meeting of the AWS *Western Michigan* Section held in the Elks Club Cafeteria, this city. Some 61 AWS members and guests spent a profitable evening listening to Mr. Ogden's lecture which was accompanied by slides. His subject was "Multi-Spot Welding in the Automotive Industry."

The usual well-prepared meal was enjoyed by some 50 members and guests before the meeting.

Electrodes

Hartford, Conn.—Charles T. Posey, AWS, Field Engineer, Arcos Corp., Philadelphia, Pa., gave an extemporaneous talk to the AWS *Hartford*, Conn., Section at its Nov. 8, 1951, dinner meeting. Mr. Posey's excellent talk on the "Development of Low-Hydrogen Electrodes," was semitechnical and covered new methods and applications as well as research.

Metallurgy

Houston, Tex.—Anton L. Schaeffler, AWS, Metallurgist, Welding Research, Allis-Chalmers Mfg. Co., Milwaukee, Wis., gave an extemporaneous talk to 142 AWS members and guests present at the November 15th dinner meeting of the AWS *Houston* Section held at the Bem Milam Hotel, this city. Mr. Schaeffler's remarks on "Simplified Metallurgy for Welders and Welding Supervisors" were research in content and excellent in quality. Many guests at the session had come a distance to hear Mr. Schaeffler point out in his talk the causes of failures in welds and the remedies therefor. A question period followed and additional problems were discussed. This was one of the most successful and well-attended meetings held to date.

During the social part of the meeting held before dinner the members and guests had an opportunity to view a movie, sponsored by the Humble Oil and Refining Co., of the 1951 Cotton Bowl football game.

The meeting opened with reports from various committees. Arrangements are in the making for the forthcoming television show this spring. After these announcements a presentation of the Society's Membership Plaque, awarded to the Houston Section for the largest number of new members during the 1950–51 season, was made to the Section. Obert Nordin, who had received the plate at the Annual Meeting of the AWS in Detroit, made the presentation. The Section is proud of this honor and the membership committee will do its best to see if they can keep the plaque another year. They are going along with their plan of inviting college students as guests to the meetings and

hope that these boys will be a nucleus for starting a student chapter later this spring.

Resistance Welding

Indianapolis, Ind.—Frank L. Brandt, AWS, Asst. Sales Mgr., Thomson Electric Welder Co., Lynn, Mass., talked extemporaneously on "Resistance Flash Butt Welding," at the Nov. 23, 1951, meeting of the AWS *Indiana* Section, held at Buckley's Restaurant, Cumberland, Ind. Mr. Brandt's talk was semitechnical and covered new information on the subject. Slides were used to illustrate his remarks and, all in all, it was an excellent presentation.

Following a lively and interesting question and answer session, samples of flash butt welding were displayed for detailed examination.

Metal Spraying

Louisville, Ky.—R. J. McWaters, AWS, Sales Manager, Metallizing Engineering Co., Inc., of Long Island City, N. Y., clearly indicated there is no conflict between welding and metal spraying in his talk given before the November 27th meeting of the AWS *Louisville* Section. Mr. McWaters' subject was the "Basic Principals of Metal Spraying" and he pointed out the limitations of both welding and metal spraying and illustrated his talk with slides indicating various usages of metal spraying.

Tooling for Automatic Arc Welding

Milwaukee, Wis.—Cecil C. Peck, AWS, President, Cecil C. Peck Co., Cleveland, Ohio, in his talk before the AWS *Milwaukee* Section on Nov. 16, 1951, at the Ambassador Hotel, this city, on the subject "Tooling for Automatic Arc Welding," pointed out that wherever large scale production is achieved, it is almost inevitably linked with automatic operations and procedures. Automatic arc welding has become a significant factor in metal product fabrication. Its development is the result of efforts to adapt arc welding to large-scale production. The speaker's fine sense of humor and his numerous anecdotes made the presentation doubly enjoyable. His many slides, portraying the vast field covered and the many ingenious jigs and fixtures, were "eye openers" to those whose work does not include this type of welding. Mr. Peck's company was formed in 1944 for the purpose of engineering and designing tools, jigs, fixtures and equipment for high-speed automatic welding of all kinds.

Jigs and Fixtures

Minneapolis, Minn.—Leslie S. McPhee, AWS, Welding Supervisor, Whiting Corp., Harvey, Ill., gave a very interesting and enlightening talk on "Jigs and Fixtures and Fabricating Cost Control" at the Monday, November 12th, meeting of the AWS *Northwest* Section meeting.

John "Andy" Anderson and fellow associates of The Minnesota Welding Supply Co. played host to 80 AWS members and guests at this meeting which was held at The Covered Wagon, this city. A thoroughly enjoyable evening was had by all.

Welded Heavy Castings

Newark, N. J.—R. A. Pomfret, AWS, Bethlehem Steel Co., Shipbuilding Division, Quincy, Mass., spoke extemporaneously at the Nov. 20, 1951, meeting of the AWS *New Jersey* Section, on the use of welding in the production of very heavy castings primarily for ship construction. He stated that some heavy castings are difficult to economically produce due to the taper required on long cylindrical sections, thin sections not capable of carrying metal to heavier sections during castings and excessive machining on large castings. He described an economical method of obtaining the final unit by making a series of two or more greatly simplified castings and joined by any of the well-known welding processes.

An added feature of the meeting was the showing of a Navy Department film entitled, "With the Marines—Chosin to Hungnan."

Steels

Oklahoma City, Okla.—Anton L. Schaeffler, AWS, Research Engineer of The Allis-Chalmers Mfg. Co., Milwaukee, Wis., gave an interesting talk on major factors to be considered when welding mild steel, high-strength steel, stainless steels and dissimilar steels, at the Tuesday, Nov. 13, 1951, meeting of the AWS *Oklahoma City* Section held at the Biltmore Hotel, this city.

Refreshments were served following Mr. Schaeffler's talk to 40 AWS members and guests present.

Navy Boats

Pascagoula, Miss.—"Welding of L.S.T.'s" was the subject of the extemporaneous talk given by Lt. Frank Sandner, Supervisor of Shipbuilding, United States Navy, at the Wednesday, Nov. 14, 1951, meeting of the AWS *Pascagoula*, Miss., meeting held at the American Legion Post, this city. Fifty-six AWS members and guests were benefitted by this talk and the motion picture which accompanied it.

Welding in America

Peoria, Ill.—An inspiring extemporaneous one-hour talk of nontechnical nature on "What Welding Means to America," treating with methods and applications, was given by T. B. Jefferson, AWS, District No. 4 Vice-President and Editor of *The Welding Engineer*, to 45 of the AWS *Peoria* Section members and guests following a dinner in the Sazarac Restaurant, this city, on Nov. 21, 1951.

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How to Reduce Costs

Phoenix, Ariz.—S. H. Taylor, AWS, West Coast Manager of the Lincoln Electric Co., Cleveland, Ohio, told the AWS *Arizona* Section on Wednesday, November 21st, what constitutes the welding dollar; power being 2 cents, electrode 6 cents, labor 46 cents and overhead 46 cents. Since power and electrode costs represent such a small part of the dollar, Mr. Taylor said every effort should be made to make labor more efficient and productive. He informed those present how larger electrode sizes, higher current densities, higher operating factors, accurate fit-up and distortion control would lower these labor and overhead costs very realistically. His informative and thought-provoking talk resulted in a lively post-talk discussion, chiefly covering the development and apparatus of low-hydrogen electrodes.

To conclude the meeting a sound film, entitled "Helpful Henry," was shown through the courtesy of the O. S. Stapley Co., Arizona representative of the International Harvester Co. The film was clever and entertaining.

Semiautomatic Process

Pittsburgh, Pa.—Charles H. Jennings, newly elected AWS President, gave a few brief remarks at the Wednesday, Oct. 24, 1951, meeting of the AWS *Pittsburgh* Section, in his first talk before a Section since his recent election as President.

Featured speaker of the technical session was J. M. Tippett, Manager, Unionmelt Eastern Region, Linde Air Products Co., New York, who discussed the semiautomatic process, or hand-operated units, as applied in the steel industry. Mr. Tippett also discussed the use of Sigma or inert shielded-gas welding as applied to the welding of aluminum, aluminum-bronze and other alloys as well as the progress made in steel fabrication. He covered the use of submerged arc welding of all steels and its application to the general steel fabrication, the steel industries and foundries. One hundred twenty-four AWS members and guests were present at the meeting held at Mellon Institute and 64 were present at dinner held in the Hotel Webster Hall.

Stainless High-Alloy Steels

Portland, Ore.—Jim Gow, AWS, Chief Metallurgist, Electric Steel Foundry Co., Portland, talked on an extremely timely and practical subject, "Types, Availability and Metallurgical Factors Affecting the Weldability of Stainless High Alloy Steels" at the Tuesday, November 13th, meeting of the AWS *Portland* Section meeting held at the Mallory Hotel, this city. The evening was an example of just how much information and knowledge could be obtained in a short space of two and a half hours. The audience participated in the discussion which followed.

Also well received was the official AWS film, entitled "Applied Welding Engineering," with sound recording; and a short discussion by Elmer C. Thylin, AWS, of the Thermit Dept. of the Metal & Thermit Corp., on the process for Thermit welding, what kind of structures were practical for this type of welding and details of how it was done.

Hard Facing

Saginaw, Mich.—W. P. Clark, President, Wall Colmonoy Corp., Detroit, Mich., described new applications of the hard facing process, at a meeting of the AWS *Saginaw Valley* Section held on Thursday, Nov. 8, 1951, in Frankenmuth, Mich., at the Zehnder's Hotel. Mr. Clark used slides and gave a demonstration in connection with his extemporaneous talk.

An after-meeting feature was the showing of a film, entitled "Buck Fever," shown through the courtesy of the Michigan Department of Conservation.

Joint Corrosion Panel

St. Louis, Mo.—The National Association of Corrosion Engineers and the *St. Louis Section*, AWS, on Nov. 9, 1951, joined in a panel discussion of "Welding for Corrosion Service in Chemical and Allied Industries." This was a live activity built around questions submitted by AWS members and St. Louis supporting companies. Two important phases of corrosion problems were discussed, that of welding corrosion resisting steels and the proper application of various alloys in the chemical and allied industries. Frank Whitney, Monsanto Chemical Co., Phil Smith, AWS, Nooter Corp., Russ Royer,

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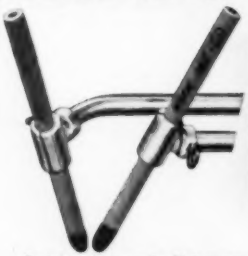

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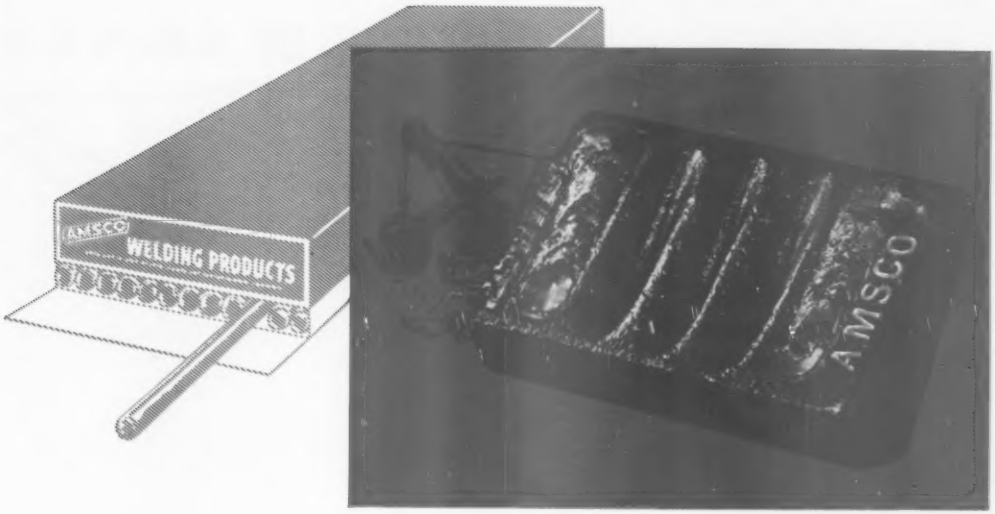

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For application to worn tooth with minimum fitting.

Straight Back
For application after worn tooth is beveled.

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Here's a new tooth repointer that is just what you've been looking for . . . the AMSCO "Wear-Sharp", *and it does exactly what the name implies.*

Note the groove design on this new repointer shown above. The outside and end grooves are AMSCOATED with Amsco Hardfacing Electrodes specially recommended for this purpose, while the inner grooves are left as they are. Result?

The Amscoated grooves wear slower, thus equalizing wear along the entire cutting edge and making the point stay sharp longer. Rounded, blunted corners that cut digging efficiency and waste power are completely eliminated for the life of the repointer!

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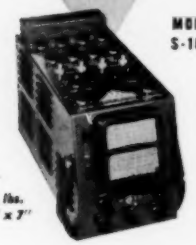

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AWS, Midwest Piping & Supply Co., and Richard Weeks, Shell Oil Co., all immeasurably contributed to this discussion, guided by Ernie Meier, former chairman of the AWS St. Louis Section, acting in the capacity of moderator. Ninety AWS members and guests gained a lot from this meeting.

The St. Louis Section also stepped out in the front by forming one of the first "Past-Chairmen's Clubs." This is an activity recommended to all Sections of the AWS as it provides automatic appointment to this honorary club of all past and future chairmen. This elite group acts as an advisory organization to all future executive committees. Neil Stueck, J. Elmo Brewer and Robert E. O'Niell were indoctrinated as new members of the executive committee, succeeding C. Herb Armstrong, Henry Weigel and DeWitt T. Harrelson to whom was acknowledged a debt of gratitude and appreciation for their faithful service as chairmen in previous years as well as members of the executive committee.

Gas-Shielded Arc Welding

Salt Lake City, Utah.—A joint meeting of the Utah Chapter, ASM, and the AWS *Salt Lake City* Section, on Thursday, Nov. 29, 1951, was addressed by E. R. Behnke, Laboratory Division Head, Linde Air Products Co., Newark, N. J., on the subject "New Developments in Gas-Shielded Arc Welding." Mr. Behnke's remarks were illustrated with slides and a high-speed (14,000 frames per second) motion picture of the Sigma welding process. Mr. Behnke is in charge of Heliarc apparatus development and also supervises laboratory participation in field promotion of gas-shielded arc-welding applications.

One hundred and seven members and guests of both societies were very much interested in Mr. Behnke's talk.

Railroad Shop Tour

South Bend, Ind.—The New York Central Elkhart Shops provided an enjoyable plant tour for 30 AWS members and guests of the AWS *Michiana* Section on Oct. 25, 1951. While the night crew was relatively small, it was very interesting to see the welding methods used to repair the "iron horses."

Aircraft Resistance Welding

South Bend, Ind.—Joseph J. Riley, AWS, Chief Electrical Engineer for the Taylor-Winfield Corp., Warren, Ohio, spoke on "Applications of Resistance Welding in the Aircraft Industry," at the Nov. 19, 1951, meeting of the AWS *Michiana* Section. Using a multitude of slides, Mr. Riley showed many types of weldments made by spot, seam or flash welding.

Brazing Demonstration

Springfield, Mass.—"Oxyacetylene Brazing of Cast Iron" was the subject of the very interesting talk and demonstration given by L. H. Hawthorne, AWS, Welding Engineer, Revere Copper & Brass, Inc., Rome, N. Y., at the Tuesday, Nov. 13, 1951, meeting of the AWS *Western Massachusetts* Section held at Jaerger Welding Supply Co., this city. Mr. Hawthorne brazed a transmission of a truck for the demonstration which was followed with keen interest.

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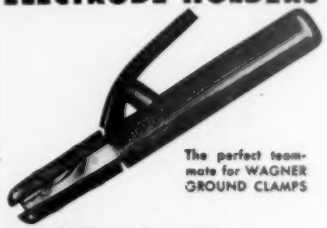
ELECTRODE HOLDERS



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Maintenance Welding

Syracuse N. Y.—Lew Gilbert, AWS, Editor of *Industry and Welding*, described the use of maintenance welding for installation and repair of equipment in industries not normally associated with welding, at the September meeting of the AWS *Syracuse* Section. The applications of welding in breweries, rubber factories, meat packing plants and candy factories were used as illustrations.

Television Plant Tour

Syracuse, N. Y.—Harry Wolfe, AWS, chairman of the AWS *Syracuse* Section was host to the AWS members who visited the Electronics Park Plant of the General Electric Co. in October. After a fine tenderloin steak dinner, the operation of a television tube plant was described. The assembly of tubes is a closely controlled operation. Of particular interest was the resistance welding of very small parts made from alloys and glass. An extra feature of the tour was a demonstration of electronic color television.

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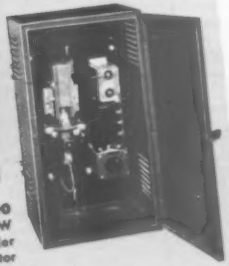
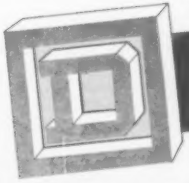
Type TCS-10 NEMA 5B
Safront Sequence Weld Timer



Class 8990
Type SCG-2 Size 2W
Syncro-Break Welder
Contactor

NEMA 3B and 5B Timers and Sizes 1W and 2W Contactors have same dimensions

Write for Class 8990 Bulletin 211 and Class 8991 Bulletin 210, Square D Company, 4041 N. Richards Street, Milwaukee 12, Wisconsin



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The World's Finest Fluxes . . .
For soft or hard soldering and brazing, ferrous and non-ferrous metals. There's a KWIKFLUX for every production need. Our 20 years experience and know-how in peacetime and wartime production improvements are at your service without obligation. Send us your problems. Write for information.
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Rigid Frame Design

Syracuse, N. Y.—Martin P. Korn, AWS, consulting engineer of Dearborn, Mich., has had about 45 years' experience in building and designing rigid frames. At the November meeting of the AWS *Syracuse* Section, Mr. Korn described many of the interesting projects with which he has been associated. He pointed out that rigid frame designed structures allow a saving in headroom, lower walls, lower heating costs and lower ventilating costs. When welded rigid frames are used, considerable reduction in material cost are often realized.

Welding Preparation

Toledo, Ohio—Charles H. Jennings, National AWS President and Engineering Mgr., Welding Dept., Westinghouse Electric Corp., Buffalo, N. Y., presented new facts, new methods and new developments on the subject at the November 15th meeting of the AWS *Toledo* Section held at Toledo University. Mr. Jennings remarks were clear cut and of general interest.

Resistance Welding

Wichita, Kan.—W. R. Plummer, AWS, Vice-President, Progressive Welder Sales Co., Detroit, Mich., also the 1951 Lecturer at the Educational Lecture Series held during the AWS Annual Meeting in Detroit, discussed the background for resistance welding, at a meeting of the AWS *Wichita* Section held on Monday, Sept. 10, 1951. Design considerations for spot, projection and seam Welding, valuation of weld quality, getting the most out of resistance welding equipment and some techniques helpful with resistance welding were all covered in Mr. Plummer's extemporaneous remarks.

Mr. Plummer has been active in the resistance welding field for 17 years. He joined Progressive in 1945, in the Planning Dept., was promoted to Chief Estimator in October of that year and Chief Sales Engineer in 1947. With the formation of the Progressive Welder Sales Co. he was appointed Vice-President in June 1950.

Structural Rigid Frames

Wichita, Kan.—"Adventures in Rigid Frame Design" is the subject dearest to the heart of Martin P. Korn, AWS, Consulting Engineer, Giffels & Vallet, Inc., Detroit Mich., and, therefore, was the subject of his talk presented at the Wednesday, Oct. 3, 1951, meeting of the AWS *Wichita* Section. Mr. Korn outlined in simple language the structural behavior of the rigid frame and then illustrated his remarks by a fast-moving panorama, sketches and slides of living structures representing Yesterday, Today and Tomorrow. Riveted as well as welded rigid frames were reviewed with interest.

Mr. Korn, member of the ASCE., is also a member of the AWS Committee on Building Codes. He has built and designed rigid frames since 1910. In addition to numerous articles published in various magazines including the "Architectural Forum" Mr. Korn has contributed material to the AWS *Welding Handbook*. Prior to coming to Detroit, he was a consultant with offices in Buffalo and New York.

Jigs and Fixtures

Wichita, Kan.—A. N. Kugler, AWS, Mechanical Engineer, Air Reduction Sales Co., New York, gave a semitechnical, illustrated talk which was excellent in quality, on the subject "Jigs and Fixtures for Welding" at the Monday, Nov. 12, 1951, meeting of the AWS *Wichita* Section. Mr. Kugler has had extensive experience in the welding of pipe for oil, gas, steam and water service. He has also participated in the development of multilayer oxyacetylene welding of carbon molybdenum steel pipe. He is author of many papers on oxyacetylene and arc welding subjects and has prepared a number of welded machinery designs.

Welding Electrodes

Worcester, Mass.—"Welding Electrodes" was the subject of the talk given by Nicholas F. Kiernan, AWS, Metal and Thermit Corp., Newark, N. J., at the Oct. 31, 1951, meeting of the AWS *Worcester* Section before an audience of 65 AWS members and guests.

Through the courtesy of the Worcester Community Film Service a sound, color film was shown before the meeting.

Stud Welding

Worcester, Mass.—Allerton Towne, AWS, Field Engineer of the Nelson Stud Welding Division, Boston, Mass., spoke on the value of "Stud Welding" in industry at the November 28, 1951 meeting of the AWS *Worcester* Section held at the Tower House, this city. In addition to a very fine motion picture showing actual applications of stud welding, Mr. Towne set up a portable stud welding set with twelve six-volt storage batteries as a source of power. The audience was permitted to try their hand at "shooting" a stud. A discussion period followed.

The social hour was followed by a Pan American Airlines travelogue in color, and a feature entitled "Split Second Fastenings," shown through the courtesy of the Worcester Chamber of Commerce.

At the Executive Committee meeting of the AWS *Worcester* Section on December 6th, it was voted to run a five night welding course at the Worcester Trade High School to be held on Tuesday nights, March 18 to April 15, 1952. Subjects to be covered are: Welding Engineering, Metallurgy, Shop Practice and Welding Symbols. Mr. John Mortimer, AWS, Welding Engineer, Whitlock Manufacturing Co., is to run the course.

Clad Steels

Youngstown, Ohio—L. W. Williams, AWS, Manager, Technical Service, Lukens Steel Co., Coatesville, Pa., described the "Manufacture and Fabrication of Clad Steels" to the AWS *Mahoning Valley* Section on November 15th at a meeting held at El Rio Restaurant, Warren, Ohio. Mr. Williams' excellent address was accompanied with a motion picture film.

For welding
Armor

McKAY ARMORLOY ELECTRODES

Write for the McKay handbook
"The Welding of Stainless Steel"

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NEWS OF THE INDUSTRY

Welding Fellowships

Two fellowships in metallurgy, each with an annual value of $3500, are available at Rensselaer Polytechnic Institute for graduate students who wish to pursue advanced work in seams and spot welding.

The fellowships have been established by the Welding Research Council, of New York City, and are to be awarded through Dr. Ernest F. Nippes, who directs welding research at the Institute. Applicants for the fellowships should get in touch with Dr. Nippes. Work may be started as soon as an award is made.

A.S.M. Schedules First Midwinter Meeting

The American Society for Metals will hold its first Midwinter Technical Meeting at the William Penn Hotel, Pittsburgh, Thursday, January 31st, and Friday, February 1st.

W. H. Eisenman, A.S.M. National Secretary, said that the decision to hold a Midwinter Meeting as a supplement to the annual meeting of the National Metal Congress and Exposition was made by the Board of Trustees as a means of discussing highly important metallurgical developments which could not be scheduled at the annual Congress because of time limitations.

Five aspects of strategic metal conservation and substitution will be discussed by top experts who are devoting special energies to advancement in these fields.

Thursday morning, January 31st, four papers will be read and discussed bearing upon new researches into Titanium and Titanium Alloys. Ten of the country's recognized authorities will be heard during this session.

Thursday afternoon, ten metallurgical specialists will present five papers covering embrittlement of both iron and steel.

A special A.S.M. Dinner will be a feature of the two-day Midwinter meet, and will be held in the Grand Ballroom of the William Penn Hotel at six in the evening on Thursday.

Following the dinner, another technical session will begin at 8 P.M. and will be devoted to the all-important subject of High Temperatures and Oxidation. Full discussions on this timely topic will follow the presentation of three papers by five leaders from government, industry and the teaching profession.

Friday morning's session at 9:30 will be devoted to metallurgy in the nonferrous field. Eight scientists from three leading universities and a government expert on light metals will deliver four separate papers covering as many technical problems faced by the makers and users of light nonferrous metals.

Friday afternoon's meeting deals with the fundamentals of Transformation and Grain Size. Thirteen qualified authorities from both industry and the teaching profession are scheduled to introduce new ideas as a result of their research and present tested methods for the study of transformation in metals.

Headquarters of the A.S.M. Midwinter Meeting will be the William Penn Hotel, according to Eisenman.

Schedule of 1952 ASTM National Meetings

March 3–7: ASTM Spring Meeting and Committee Week. Symposium on Testing Metal Powders and Metal Powder Products, Cleveland, Ohio (Hotel Statler).

June 23–27: ASTM Annual Meeting—Fiftieth Anniversary. Exhibit of Testing Apparatus and Laboratory Supplies, and Photographic Exhibit, New York, N.Y. (Hotels Statler and New Yorker).

Flame Cutting Cast-Steel Risers

Risers are removed rapidly from cast-steel spur gear blanks in a mid-western foundry. An Oxweld cutting blowpipe is used to cut off 14-in. wide risers at a rate of $5^1/_2$ in. per minute. The use of an oversize nozzle at a low-oxygen pressure for riser cutting has been found to produce smoother cut surfaces at higher speeds.

The blowpipe is mounted on a mechanized cutting machine which rides on a circular track plate. The cutting machine is guided by a radius rod attached to a permanently located center point.

First, the risers are severed in the horizontal plane as shown in the photograph. After this operation is completed, the riser feet shown on the periphery of the casting are removed with the same apparatus. This requires merely relocating the cutting nozzle to the vertical position by adjusting the bevel cutting adaptor.

Meeting of German Welding Society

The German Welding Society (Deutscher Verband für Schweisstechnik Ev.) has announced that its principal meeting in 1952 will be held in Essen, in conjunction with the D.V.S. Exhibit on Welding and Cutting.

The Exhibit is scheduled to open on Saturday, June 12, 1952, and will continue through Sunday, June 29th. It will be devoted to a comprehensive presentation of all phases of welding in fabrication and other applications.

At the meeting, outstanding experts will report on welding as used in the shipbuilding, mining and chemical industries, as well as in machinery fabrication, steel construction and bridge building.

Flame Cutting Cast Steel Risers

H. W. Gillett Memorial Lecture Established by ASTM

The American Society for Testing Materials has announced the establishment of an annual H. W. Gillett Memorial Lecture. The purpose of this lecture, which is being sponsored in cooperation with Battelle Memorial Institute, is to commemorate Horace W. Gillett, who was one of America's leading technologists, the first Director of Battelle in Columbus, Ohio, and an active worker for many years in ASTM.

The memorial lecture will be delivered annually at a meeting of the Society, the first to be given at ASTM's 50th Anniversary Meeting in New York City during the week of June 23rd. The lecturer, who will be selected through a committee appointed by the ASTM Board of Directors, will cover a subject pertaining to the development, testing, evaluation and application of metals. Dr. Gillett was intensely interested in these fields, perhaps particularly in a critical evaluation of metals and alloys.

A member of ASTM for over 25 years, Dr. Gillett served on many of its technical committees, notably in the field of metals, contributing numerous outstanding technical papers and reports to the ASTM publications. He was a most able writer and couched his material in a unique and effective style.

Dr. Gillett, who died in March 1950, was an active member of many technical and scientific groups and had received numerous honors and awards.

Welded Bridge Program to Conserve Steel

Welded bridges have been built in the United States which have made possible savings in steel ranging from 5 to 20% of the total tonnage that would have been required had the bridge been riveted. To encourage such steel savings and insure a maximum total national saving in the present emergency, the Lincoln Arc Welding Foundation is sponsoring a 1952 Award Program, "Welded Bridges for Steel Conservation."

The Program is open to all persons in the United States who feel themselves qualified to enter and offers 15 awards totaling $16,100. First award is $7000, second award $3500, third award $2000 with 12 honorable mention awards of $300 each. Awards will be given for the best bridge designs which show weight and cost savings over a comparable riveted bridge. The Program allows entrants complete freedom as to size and type of bridge to be designed since the percentage of saving is the most important factor to be judged.

Rules for this Program, which is dedicated to the national interest through the conservation of steel, have been formulated by 14 of the nation's leading bridge engineers, 5 of whom will serve as Jury of Award. Chairman of the Program is Professor James G. Clark of the University of Illinois. Rules are available from The James F. Lincoln Arc Welding Foundation, Cleveland 17, Ohio.

PAGE
Stainless Steel
WELDING ELECTRODES
Armor Welding Electrodes
. . . and All Standard Types
AC or DC . . . the BEST
. . . for any weld
on all types
of stainless steel
STAINLESS STEEL
has great strength . . . but its strength is limited by the quality of welds which seal the joints. In all kinds of manufacturing—military work in particular—stainless is being used more widely. If you weld stainless, choose electrodes with care.
PAGE STAINLESS STEEL ELECTRODES, AC or DC, give a stable arc under all conditions. The metal flows smoothly. Slag is clean and easily removed. The coating resists cracking down to very short stubs. Now available in 10-lb. lined, hermetically sealed metal cans which can be reclosed. Be sure . . . specify Page Stainless Steel Electrodes.
ACCO
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PAGE STEEL AND WIRE DIVISION
AMERICAN CHAIN & CABLE
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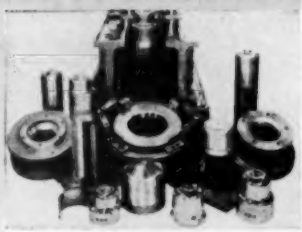
300% more life from dies



overlaid with Ampco Bronze Electrodes

Production was increased from 3600 deep-drawn cases (for sealed refrigeration units) to 15,000 cases, when Industrial Stamping and Manufacturing Co. of Detroit overlaid their steel punches and die-rings with Ampco Bronze electrodes.

Big Savings in Press Room

Downtime was greatly reduced and operating time increased. Efficiency was upped 15% because presses did not have to be stopped to hone and polish dies or change tools in the middle of a run. Average production was increased to 15,000 units before appreciable signs of wear on the dies.

Put Ampco Bronze electrodes to work for you. Enjoy longer production runs, less downtime, lower costs.

Industrial Stamping and Mfg. Co. personnel inspecting punch overlaid with Ampco Bronze electrodes.

W-113

AMPCO **Ampco Metal, Inc.**
Milwaukee 46, Wis.
It's Production-Wise to Ampco-ize!

Black Binder Found

A small black binder with alphabetical index and some notes in it was found during the AWS Annual Meeting at the Book Cadillac Hotel, Detroit, the week of October 14, 1951. Contact the Editor, THE WELDING JOURNAL, 29 West 39th Street, New York 18, N. Y.

Booklet on Photoelastic Stress Analysis

The Eastman Kodak Co., Rochester 4, N.Y., announces that a completely revised and up-to-date edition of "Photoelastic Stress Analysis" is now available through Kodak dealers.

This new booklet, like the old, begins by pointing out the effectiveness of photoelastic stress analysis for the purpose of solving problems of stress distribution. It discusses briefly other methods of stress analysis and then launches into a discussion of the principles of photoelasticity. This section covers polarized light, double refraction or birefringence, isochromatic lines, isoclinic lines, circular polarization, and stress patterns.

The booklet then discusses apparatus for the production of photoelastic stress analysis including light sources, filters, polarizers, analyzers, quarterwave plates, condensers and the camera.

Materials which are suitable for making models of the object or part to be tested are discussed and the actual making of the models and factors that should be considered therein are covered. Models for three dimensional stress analysis are also discussed.

Photographic materials for use in the process, exposure and processing, and a list of manufacturers who make equipment for use in photoelastic stress analysis are also covered in the publication.

Finally, there is a bibliography of books and articles on the subject.

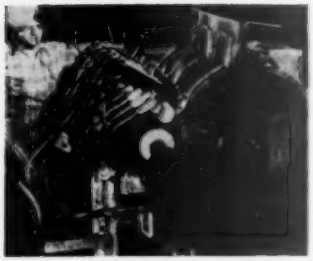
"Photoelastic Stress Analysis" is illustrated in both black-and-white and color with numerous pictures and diagrams. It sells for 35 cents a copy and will be available through Kodak dealers.

Flame-Hardening of Tractor Idlers

Cast steel tractor idlers with 0.42 to 0.48 carbon and 0.65 to 0.85 manganese content are efficiently flame-hardened by oxyacetylene flames at Harrison Steel Castings Co., Attica, Ind. This treatment lengthens the life of idlers by increasing the hardness of the steel from approximately 200 Brinell to approximately 500 Brinell with a $^1/_8$ in. depth of hardness.

Six heating heads (two for each idler), specially made for the job, are hooked up to Oxweld blowpipes. The operator merely throws a switch on the shop-made setup to start the operation. In a few minutes, the idlers are hardened.

The idlers get a hard, wear-resisting surface with the tough, shock-resistant core unaffected. Flame hardening is also advantageous in that it is a rapid, economical method. There is no change in the chemical analysis of the finished product, and no sharp demarcation between the hardened zone and the softer core.



Flame-Hardening Lengthens Life

Eutectic Expands Research Laboratories

The construction of a new wing to house two new research laboratories has been completed by Eutectic Welding Alloys Corp., Flushing, N. Y.

The new building, it was reported, will contain a special staff of research chemists and physicists who will concentrate on the development of new welding alloys utilizing fewer critical metals in today's shortage-burdened market. Research on new metals, such as titanium joining, is also included among the projects scheduled for this group.

Babcock & Wilcox Tube Co. Dissolved

The Babcock & Wilcox Tube Co., a wholly owned subsidiary of The Babcock & Wilcox Co., will be dissolved, effective at close of business December 31st. The Tube Company's business and assets will be absorbed by, and it will be operated as, one of the divisions of The Babcock & Wilcox Co. thereafter, according to an announcement made in New York on December 3rd.

The announcement, made jointly by the boards of directors of both companies, emphasized that the transfer of the plants and business of the Tube Company will have no effect upon its continued operation so far as the customers and employees of the subsidiary are concerned and will not change The Babcock & Wilcox Co. stockholders' equities. The tube facilities will be operated as the Tubular Products Division of the company and Luke E. Sawyer and Edward A. Livingstone, Tube Co. officers, will also become officers of The Babcock & Wilcox Co. The announcement said the change would be beneficial from an organizational and managerial standpoint.

The Babcock & Wilcox Tube Co. has its main offices at Beaver Falls, Pa., with plants at Beaver Falls and Alliance, Ohio. It manufactures seamless and welded tubing in carbon, alloy and stainless steels for a wide variety of industrial purposes.

GOOD PRACTICE

WASTEFUL

CARE IN HANDLING, GOOD WELDING TECHNIQUES REDUCE ELECTRODE WASTE

You can get factory-fresh performance from your electrodes through good storage and protection practice. Don't waste electrodes by leaving them in damp corners or on floors where they can become saturated with moisture, stepped on, chipped, bent, or otherwise rendered useless.

Here are some other "ground rules" to follow to get up to 40% more from your electrodes:

1. **Avoid bending whenever possible**
2. **Use electrodes down to a 2-inch stub**
3. **Provide better fit-ups**
4. **Lay flat-face fillets on vertical fillet welds**
5. **Make fillet legs of equal length on horizontal fillets**

To get the most from your electrodes, get the best electrodes . . . specify General Electric. G-E electrodes give you high deposition rates, a smooth stable arc, meet AWS specifications and are available in a wide variety of sizes and types.

HERE'S A TIP! Stamp the date received on each electrode carton and use those with the earliest dates first.

FOR THE BEST IN ACCESSORIES . . .

See your authorized General Electric Welding Distributor. He carries a complete line of G-E accessories, including electrode carriers, electrode holders, helmets, lenses and cover glasses, hand shields, leather protective clothing, brushes, ground clamps, cable connectors and many other items.

Your G-E Welding Distributor can also give you additional hints on how to get the most from your electrodes and equipment. You can find his name in the yellow pages of your telephone directory. Look for "General Electric" under "Welding Equipment." General Electric Company, Schenectady 5, N. Y.

GENERAL ELECTRIC

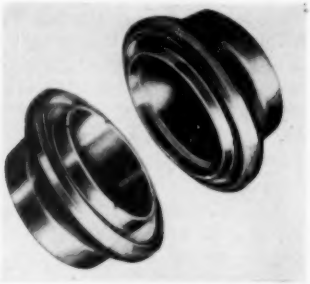
712-12

NEW PRODUCTS

Scale-Free Welding Coupling

A new product, the Tube-Turn SF (Scale-Free) Welding Coupling, now makes it possible to eliminate damaging welding scale from piping systems, according to Tube Turns, Inc., of Louisville, Ky.

The Tube-Turn SF (Scale-Free) Welding Coupling consists of two forged halves, as can be seen in the accompanying photograph. The ends of the hubs are prepared for welding to pipe or welding fittings of corresponding sizes. When the two halves of the coupling are brought together, the tongue of one slips into the groove of the other. A circumferential cavity directly beneath the beveled welding area prevents burn-through or the formation of icicles in the interior. The cavity also insulates the interior from the extreme welding heat and prevents scaling.



The Tube-Turn SF (Scale-Free) Welding Coupling is useful in connecting piping designed to carry hydraulic fluids, high-pressure steam, compressed air, certain chemicals and gases, lubricating oil, fuel oil, carbonated beverages (including beer) and various food products. It is available in sizes from 1¼ through 12 in. and in carbon steel, stainless steel, aluminum and other metals and alloys.

Complete information about the Tube-Turn SF (Scale-Free) Welding Coupling may be obtained from the manufacturer Tube Turns, Inc., Louisville 1, Ky.

Flexible Round File

Tec Imports announces the availability of a flexible, round file. The "Abrafile," as it is called, will file any material of any profile by bending the tool to the desired shape. Nonclogging, will not fill up on lead, wood, aluminum, cardboard or plastic materials. Abrafile comes in six sizes 3/32 to 1/4 in. For further information write to Thriftool Sales Co., 122 E. 25th St., New York 10, N. Y., or to Tec Imports, 14404 Addison St., Van Nuys, Calif.

Soldering and Brazing Unit

A new type soldering and brazing unit has just been developed by the Metallizing Company of America, Chicago. Said to be adaptable to any type of manufacturing where soldering or brazing is required, the unit—called the Mogul Soldering or Brazing Gun—is completely suited to special soldering jobs as well as production runs.

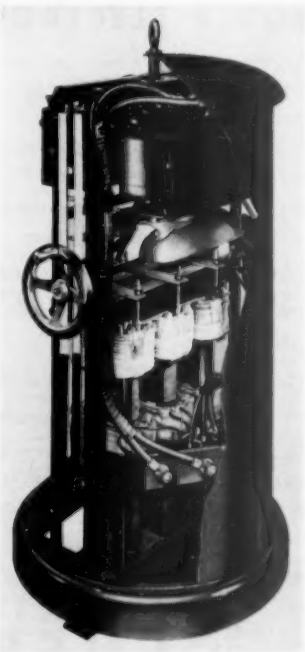
The Mogul Gun, according to the manufacturer, is built to deposit lead and tin base solders, and also silver solder and other brazing wires, in a liquid or semi-liquid form to a part moving at constant speed under the gun nozzle. Soldering or brazing can be either intermittent or continuous at any desired rate of speed; speeds of up to 200 lineal feet of seam per minute are permitted.

The Mogul Gun will supply soldering material in any desired quantity which can be deposited to a pin-point location. This material, in wire form, is fed into the center of a conical flame where it is melted into a liquid or semiliquid state, depending upon requirements, and then deposited into the seam or area being soldered or brazed.

Full information and literature on the Mogul Soldering and Brazing Guns may be obtained by writing direct to the Metallizing Company of America, 3520 W. Carroll Ave., Chicago 24, Ill.

Arc-Welding Machine

A new, heavy-duty direct-current welding machine has been put on the market by the A. O. Smith Corp., Milwaukee, Wis. The machine is designed for all industrial uses, where d.-c. welding is required.



NEW LINCOLN PLANT CREATED BY INCENTIVE-INSPIRED CO-ACTION IN DEVELOPING POSSIBILITIES IN PRODUCT

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BUILDS LIGHTER, STRONGER PRODUCTS AT HALF THE COST

By **Robert F. Christian,**
General Manager, J. D. Christian Engineers
San Francisco, California

ORIGINALLY, our gearmotor housings were made from gray iron. However, during World War II we developed many techniques in welding these gear cases that are saving materials, manpower and shop cost. Among the advantages gained by our conversion to welded steel are the following benefits:

1. Savings in weight alone have proved important to equipment builders who use our products and who must ship their machinery "prepaid" to all parts of the country. The weight savings also allow lightening up supporting structures to save construction costs of our customers' products.

2. Shorter shop schedules are improving our own deliveries by eliminating the demand for core work formerly required on our original designs and which was not attractive to suppliers.

3. With welded construction, our products are more adaptable for use on special machines. For example, the relocating of mounting bolt holes is now easier.

4. Ninety per cent of the machining time formerly required has been eliminated.

Conservatively speaking, our shop turns out comparable equipment for about half of the original cost in gray iron.

Similar savings are undoubtedly possible on many of your present and proposed products. A Lincoln Welding Engineer will gladly work with your designers to show how you can benefit with welded steel. Call or write.

PROPER DESIGN IN WELDED STEEL ALWAYS IMPROVES PRODUCT AND LOWERS COSTS

Fig. 1. Original Construction of gear motor housing. *Required 90% more machining.* Weighed 175% more than welded steel.

Fig. 2. Present Weldesign in Steel. *Costs 50% less.* Further saves shipping costs "prepaid" by manufacturers, using the product.

HERE'S PROOF

Machine Design Sheets are available to designers and engineers. Simply write on your letterhead to Dept. 91,

THE LINCOLN ELECTRIC COMPANY
CLEVELAND 17, OHIO

To safeguard against stack failure, a high-velocity downdraft of cool air is directed over the rectifier stacks before passing this air through other parts of the machine. The blast is expelled at the base of the A. O. Smith welder. This not only supplies proper cooling action, but promotes internal machine cleanliness.

A. O. Smith retains in this d.-c. welder all of the principal construction features of its Heavy-Duty A.-C. welder. Among these features are the case-diameter fan and "wind tunnel" design to provide air flow over all energized parts. The primary coils are raised and lowered on ball bearing jacks. The machine is available in 200, 300 and 400 amp. ratings.

Clip-On Goggles

The Pennsylvania Optical Co. announces that it is placing on the market clip-on goggles for industrial workers who wear prescription safety or welding glasses. These new type goggles are intended to protect the more expensive lenses against sparks and flying bits of metal. They are light in weight and will fit over either shell or metal framed glasses.

The Penoptic clip-on lenses are made of clear glass or extra dark absorptive glass. They are available in 44 mm. and 46 mm. sizes.

The clip-on goggles are so constructed as to enable the replacement of either lens if necessary.

Alloy Steel Welding Pipe Fittings and Flanges

Key Co., of East St. Louis, Ill., manufacturers of alloy steel castings for high pressures and temperatures, has announced the development and production of a complete line of "Key-Kast" Alloy Steel Welding Pipe Fittings and Flanges. The company states their new line provides a stronger fitting and gives longer life to piping systems at a lower unit cost. Also, it claims that the new Key-Kast design offers these important advantages to fabricators and users of alloy pipe:

1. Greater wall thickness throughout, with added thickness at all areas of stress, provides greater structural strength and a larger allowance against corrosion and erosion.
2. The new line meets A.S.A., ASME and ASTM standards.
3. Available in low and intermediate alloys and various types of stainless steel.
4. A boss is provided on all fittings for tapped openings.

The new Key-Kast line of alloy welding fittings and flanges is available in all sizes and schedule numbers.

Complete information on Key-Kast Alloy fittings and flanges may be obtained from Key Co., East St. Louis, Ill.

LIL CHAMP
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IT'S SURELY GOING TO BE TOUGH! THE WELDING HAS TO BE GONE OVER WITH A FINE TOOTH COMB— OUR CUSTOMER'S DEMAND AN A-1 JOB —
I DON'T KNOW HOW TO PUSH THE BOYS ANY HARDER!
WAIT'LL THEY START USING THAT CHAMPION BLACK DEVIL — THEY'LL GET THAT INCREASED PRODUCTION EASILY WITH THEIR SAME MANPOWER
THOSE WELDERS ARE SURE A HAPPY CREW. WITH THAT FAST-BURNING HIGH DEPOSIT RATE CHAMPION BLACK DEVIL THEY'RE BATTING A THOUSAND ON THAT PRODUCTION SCHEDULE!
NO UNDER CUT ON THE EDGES- PRACTICALLY NO SPATTER TO CLEAN OFF — SLAG CAME OFF EASY BETWEEN PASSES!
THE APPEARANCE OF THE WELDS IS FIRST RATE, SMOOTH, EVEN AND THOSE X-RAYS SHOW PERFECT!
THE FASTEST HOT ROD ON THE MARKET PROVES ITSELF AGAIN!
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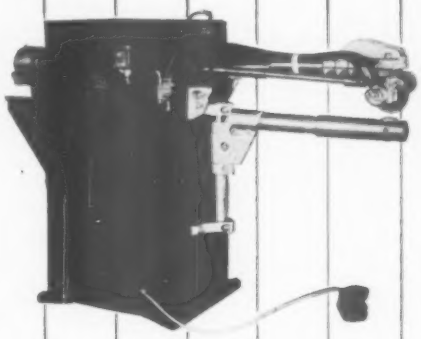
Banner's SEAM WELDERS

SPECIALLY MADE TO MEET THE NEED FOR A SMALLER SEAM WELDER



BANNER 50 KVA ROCKER ARM SEAM WELDER

Here is a Banner Rocker Arm Seam Welder built to handle up to 20 gauge mild steel and lighter—a job usually requiring larger, more costly machines. This 50-KVA Model has 24" throat with the upper wheel driven through shafts and pinion gears. Positive drive originates from a Graham vari-speed transmission mounted at the rear of the machine ● Shafts are securely mounted in floating bearings with universal joints. The machine shown above has a lower horn for longitudinal welding, but is easily converted to circular seam welding by installing a special lower horn and rotating the upper head 90 degrees (merely loosen four bolts and re-connect the drive from the Graham speed reducer) ● Transformer is fully water-cooled and rated at 100% duty cycle to handle a multitude of applications on high production 20 gauge (and lighter) mild steel. *Write for details!*

30 KVA CIRCULAR WELDER

This seam welder is specially designed for use on lighter materials (26 and 28 gauge mild steel). It has many outstanding features usually found on larger, more expensive machines ● Lower wheel is driven by small fractional horsepower motor through gears ● A multitude of speed combinations and ratios are quickly and easily made by changing gears ● Positive drive (gear driven) ● All current-conducting members are of high conductivity, high strength materials ● Transformer is fully water-cooled and rated at 100% duty cycle ● Upper and lower shafts are also water-cooled, facilitating high production ● Upper ram follows Banner's pattern of rugged construction with a long bearing surface of 7" ● Ram is manganese bronze, running in cast iron and guided by a $\frac{5}{8}$" pin ● Frame is $\frac{1}{4}$" boiler plate, with $\frac{5}{8}$" reinforced face plate.

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Banner manufactures a complete line of Gun, Seam, Rocker Arm and larger Press Welders and Specials to 200 KVA. Inquiries invited.

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abstracts of
CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 10, D. C.

2,574,100—Mounting for Cutting Torches—Harold R. Gettys, North Bend, and Russell P. Kissick, Cincinnati, Ohio, assignors to Stacey Brothers Gas Construction Co., Cincinnati, Ohio, a corporation of Ohio.

A device for trimming the edge of a metal plate is covered by this patent. It includes a carriage for supporting a plurality of cutting torch nozzles in angular relationship to each other and the edge of the plate. The carriage is mounted for free swiveling movement about a horizontal axis and other means mount the carriage so that it floats and swivels in accordance with the tracking contact of wheels provided for the carriage. The apparatus permits the position of the nozzles to remain substantially the same relative to the edge of the plate as variations in the wheel contact surface occur.

2,574,101—Machine for Thermochemically Severing Metal Workpieces—William J. Greene, Plainfield, N.J., assignor to Air Reduction Co., Inc., a corporation of New York.

Greene's machine includes a frame with a pair of upper and lower arms pivotally connected to a pair of uprights to form a pantograph structure, the upper arm being a torch-carrying arm and the lower one being a control arm. A pivotal connection is provided between a main supporting arm and the frame. A gas cutting torch is mounted on the torch-carrying arm and means connect to the control arm to apply a vertical component of movement to a region of such arm while other means operatively connect to the control arm and operate independently of the previously named means to apply a horizontal component of movement to such region of the control arm.

2,574,514—High-Frequency Unit for Inert Gas Arc Welding—Charles L. Volff, Montreal, Quebec, and Julius Hammerslag, Outremont, Quebec, Canada, assignors to L'Air Liquide Societe Anonyme Pour l'Etude et l'Exploitation des Procedes Georges Claude, Paris, France, a body corporate.

An electric welding apparatus including an electrode holder and means for furnishing welding current to such holder, together with electronic means for furnishing continuous high-frequency current to the holder is disclosed in this patent. The electronic means include a generator having a vacuum tube oscillator stabilized by a crystal and amplified by at least one amplification circuit. The electronic means produce high-frequency current in a band of frequency permissible for industrial apparatus. Means are provided for tuning the entire electronic circuit for furnishing welding current to obtain a maximum power output at the end of the welding circuit adjacent to the electrode holder for striking the arc. The tuning also provides for a minimum of radiation at the welding circuit.

2,573,020—Arc Welder Interrupter—Arthur H. Fessler, Napoleon P. Boretti and Karl Sarafian, Detroit, Mich., assignors to General Motors Corp., Detroit, Mich., a corporation of Delaware.

This patent is on a control system for welding means wherein switch means connect a power source to a welding electrode. A plurality of time delay relay means are connected in sequence and to the power source while a second switching means is operated by the final time delay relay and controls the first switching means. A control relay is connected across one of the time delay relays and manual switching means are connected in circuit with the control relay so that the timed sequence of operation may be initiated manually.

2,573,094—Electric Welding System—Irvin W. Cox, West Allis, and Jerome B. Welch, Milwaukee, Wis., assignors to Cutler-Hammer, Inc., Milwaukee, Wis., a corporation of Delaware.

This patented welding system includes a transformer with welding electrodes connected to the secondary thereof. Half wave rectified current is supplied to the primary of the transformer and means supply an alternating demagnetizing current of low magnitude to the primary when the half wave rectified current supply means are nonconducting. A high resistance is connected in parallel to the electrodes to provide a discharge path for the current induced in the second transformer by the demagnetizing current.

2,573,530—Method of Making Welded Steel Units—James P. Zallea, Wilmington, and John J. Mank Newark, Del., assignors to Zallea Bros., Wilmington, Del., a partnership consisting of James P. Zallea and Sol Zallea.

This patent covers a specialized reinforcing unit for expansion joints of the type having a corrugated flexible tube and it primarily is on the mechanical construction of the unit.

2,573,901—Arc Welding System—Dean C. Girard, San Leandro, and Frank T. Roach, Hayward, Calif., assignors, by mesne assignments, to National Cylinder Gas Co., a corporation of Delaware.

This patent relates to the energization of an intermittently loaded power circuit wherein a transformer is connected to a power source and circuit means connect the first relay for energization from a separate current source in series with the power circuit when the power circuit is short circuited, and a line contactor connecting the primary of the transformer to the power source and opening a second relay is provided and circuit means connect it for energization as a result of the energization of the first relay. When the short circuit is removed and the first relay is de-energized, the line contactor is then energized.

2,574,939—Welder Control System—Harris I. Stanback, Milwaukee, and Ernest G. Anger, Wauwatosa, Wis., assignors to Square D Co., Detroit, Mich., a corporation of Michigan.

This new control system relates to the sequence of welding operations. The patent covers electrical controls for a valve solenoid which can be energized to apply pressure to the welding electrodes; means disconnect part of the electric control to initiate the squeeze time period while pressure switch responsive means and timing means are present for use in controlling the welding functions. An adjustable timing means also is provided to control the number of conducting periods of portions of the electric periods to determine the "weld time" period.

2,575,381—Method of and Apparatus for Continuous Welding of Pipes and Tubes—Irving A. Colby, Hubbard, Ohio, assignor to The Youngstown Sheet & Tube Co., Youngstown, Ohio, a corporation of Ohio.

The method of this patent is for continuously producing welded pipes and includes passing a continuous skelp strip progressively through a heating chamber to raise the skelp to heating temperature. Next the skelp is progressively converted to substantially cylindrical form and adjacent edges of the skelp are then subjected to a higher frequency electrical field to raise the edges of the skelp at least to welding temperature. Substantially simultaneously with the high-frequency field, the edges of the skelp are formed together to produce a longitudinally continuous welded seam.

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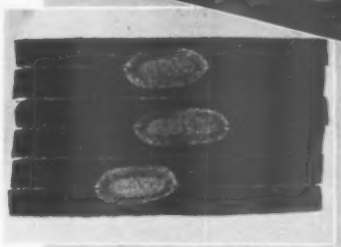

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2,575,736—METHOD OF THERMOCHEMICALLY CUTTING METALS—Jesse S. Sohn, Succasunna, N.J., assignor to Air Reduction Co., Inc., New York, N.Y., a corporation of New York.

Sohn's thermochemically cutting method comprises positioning torch means having a row of closely spaced cutting oxygen orifices in operative position with respect to the work to be cut and positioned to extend in the direction of a desired cut throughout the cutting operation. A jet of cutting oxygen is projected onto the workpiece from the first of the orifices to thermochemically remove metal therefrom and thereafter a jet of cutting oxygen is projected from a second orifice immediately adjacent said first orifice and downstream therefrom in the direction of the cut. Thereafter successively and progressively projecting jets of cutting oxygen are fed from the orifices and the jet of oxygen is discontinued from the preceding orifice as succeeding jets are supplied with cutting oxygen.

2,575,737—GAS TORCH MACHINE FOR CUTTING ROUNDS AND THE LIKE—Joseph M. Tyrner, New York, N.Y., assignor to Air Reduction Co., Inc., a corporation of New York.

A portable cutting machine of the gas torch type for severing rounds is disclosed in this patent. The machine includes a base that can be clamped to the round to be cut and a post disposed perpendicularly to the base and axially rotatable thereon. A cutting torch having a tip is supported by parallelogram linkage from the base. Other control means are provided so that the torch can move in an arcuate path at right angles to the longitudinal axis of the workpiece.

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Funk, Edward Ross (C)
Pote, Lloyd W. (B)

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Hilte, Leonard (B)
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Staiano, E. (B)
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Pratt, Charles J. (B)

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Homany, William (C)
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Tatro, Russell (C)
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Vojtech, Ladislav (C)
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Wolfenden, William O. (C)

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Cupp, William F. (C)
Denzel, Frank P. (C)
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Martin, David C. (C)
Meyer, Richard (C)
White, Peregrine (C)

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Mays, George Q. (C)
Moss, Clifton L., III (C)
Nelson, Gerald M. (D)

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Sharp, Chester C. (C)
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Wagner, A. D. (B)
Width, Howard (B)
Width, Robert B. (B)

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Fution, Harold R., Jr. (B)
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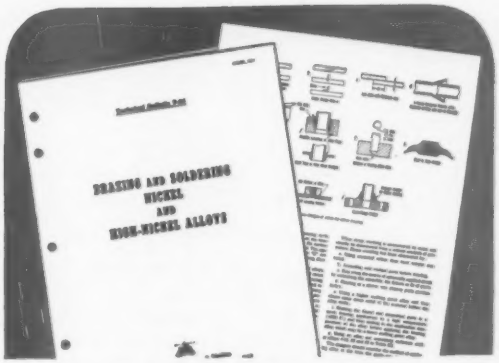
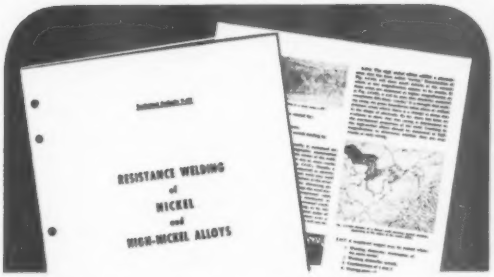


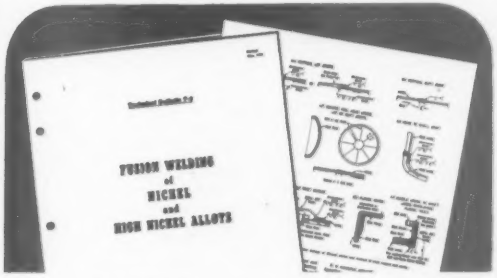
T-34, "Brazing and Soldering Nickel and High-Nickel Alloys"

This is the newest welding publication from Inco's Technical Service Section. It covers design, surface preparation, fluxes, heating methods, post brazing treatment and inspection for silver and copper brazing. It also contains valuable tables on properties and formulae and a section on soft soldering.



T-33, "Resistance Welding of Nickel and High-Nickel Alloys"

T-33 covers design and equipment considerations, welding procedures (both for similar and dissimilar metals), checks for defects and tests for spot, seam, projection and flash welding. It also covers resistance brazing. It gives tables on chemical analysis and mechanical and physical properties, and recommended conditions for welding. The text is amply illustrated with valuable diagrams and photographs.



T-2, "Fusion Welding of Nickel and High-Nickel Alloys"

T-2 covers surface preparation, joint designs, jigs and clamps, electrodes, current, pre-heating, flux and welding procedures for metal arc and gas arc welding. It discusses overlays on cast iron and joining of dissimilar metals; and the preparation, application and testing of linings. It contains tables of chemical analysis and mechanical and physical properties and electrode requirements. Like T-34 and T-33, it is heavily illustrated with diagrams and photographs.

Inco's Technical Service Section wants you to have all three of these bulletins to complete your library on the most up-to-date welding techniques. There is no charge for them. Write today for T-34 and if you don't have T-33 and T-2 ask for them, too.

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Supplement to the Welding Journal, January, 1952

Predetermination of Preheat and Postheat for Submerged-Arc Welding

Comparison of the transformation of austenite with continuous cooling and applications of controlled preheat and postheat treatments in welding alloy steels

by Clarence E. Jackson and Arthur E. Shrubsall

INTRODUCTION

OVER the years, metallurgical developments have been watched carefully by the welding fraternity, since often these developments have had a direct application in the solution of welding problems. Probably the widest application of metallurgical concepts in welding circles has been the study of the effect of welding on the properties of the heat-affected zone. Although welding generally subjects a particular structure to higher temperatures, with a shorter duration than normally encountered in heat-treating practices, still the explanations proposed by metallurgical engineers have been widely used as tools by the welding engineer.

The past decade has seen the development of lime-type coated ferritic electrodes which have proved useful in metal-arc welding of hardenable steels. This type of electrode is not subject to the heat-affected zone cracking associated with coatings having hydrogen-producing constituents. Since the welding composition used in the Unionmelt* (submerged-arc) welding process does not contain hydrogen-producing ingredients, and since high currents and high speeds with improved efficiency[1] are normally employed, this process is readily applicable in welding steels such as S.A.E. 1040 or steels with even higher carbon in moderate thicknesses.

In spite of the vast amount of effort, which has been expended in the development of welding techniques for hardenable steels, using no preheat or postheat, many applications in medium-alloy steels would give better metallurgical structures if proper cognizance were taken of the rates of metallurgical response in the heat-affected zone. In the welding of materials of the hardenable type in thicknesses greater than $^1/_2$ in., it is often expedient to employ multipass techniques with the Unionmelt process, and the use of controlled preheat, interpass and postheat temperatures[2-4] often gives added safety with improved properties in the heat-affected zone. The indiscriminate use of preheating and postheating, however, can lead to structures with high hardnesses and reduced ductility. Only through an understanding of the effects of heat-treatment practices can sound crack-free welds in hardenable steels be assured.

THERMAL CYCLE IN HEAT-AFFECTED ZONE

A full understanding of such an evaluation requires a knowledge of the basic metallurgical changes which may be encountered. It will be worth while to follow through these steps, not so much to present new information, as to bring together and emphasize the salient points. Much has been written[4-6] as a result of extensive studies of the thermal history adjacent to welds. Other attempts have been made to use these data for evaluating the weldability of steels.

In order to provide a survey of the thermal cycles encountered, first, let us consider a surface bead deposited on a $^1/_2$-in. thick S.A.E. 1050 plate with a cross section of this weld as shown in Fig. 1. The hardened zone, shown by the dark etched area in the plate adjacent to the weld bead, can readily be seen. This

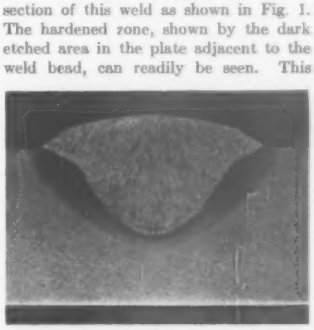

Fig. 1 Cross section of bead weld deposited on $^1/_2$-in. thick S.A.E. 1050 steel. Welding technique: 600 amp., d. c.-r. p., 30 v., 20 in. per min. travel, Unionmelt composition grade 50, (12- by 150-mesh). Oxweld No. 36 electrode, $^3/_{16}$-in. diameter

Clarence E. Jackson and Arthur E. Shrubsall are connected with the Union Carbide and Carbon Research Laboratories, Inc., Niagara Falls, N. Y.

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* The terms "Oxweld" and "Unionmelt" are registered trademarks of Union Carbide and Carbon Corp.

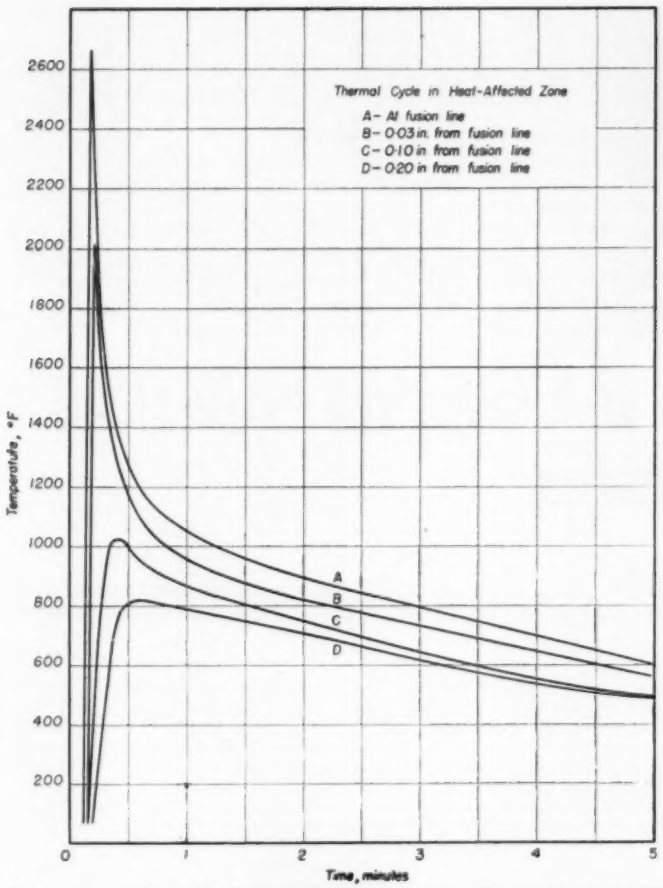

Fig. 2 Thermal cycles in heat-affected zone at center line under bead weld shown in Fig. 1

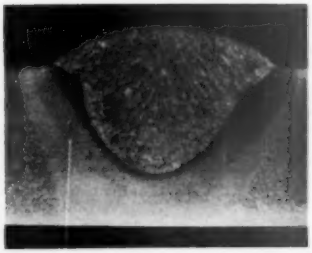

Fig. 3 Cross section of bead weld deposited on 1/2-in. thick S.A.E. 1050 steel. Welding technique: 725 amp., d. c.-r. p., 30 v., 20 in. per min. travel, Unionmelt composition grade 50

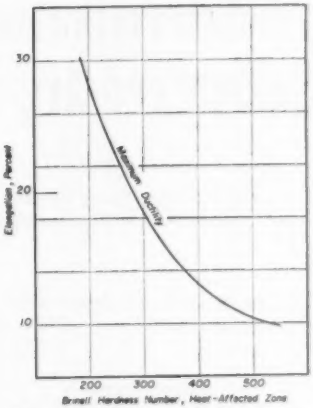

Fig. 4 Relation of hardness and ductility in steel. Maximum ductility is that for fully heat-treated steels. Area to left of curve represents ductility which may be obtained for heat-affected zones

hardened zone results from the effects of the thermal history of each particular point in the steel. Fig. 2 shows the thermal cycle to which the various structures have been subjected. These thermal cycles apply only for the area at the center line below the weld and for the particular geometric arrangement under consideration, as weld beads with a smaller nugget area will show faster cooling rates in the heat-affected zone, and larger nugget areas will show slower cooling rates. The maximum temperature at the fusion line, in all cases, will be that of the melting temperature of the base metal. With the smaller beads, the heat-affected zone follows the contour of the fusion line. As the weld nugget becomes larger or the plate thinner, a condition is reached in which the heat-affected zone extends through the entire thickness of the plate (Fig. 3). As the heat-affected zone becomes wider, the temperature gradient is less drastic, although the volume in the heat-affected zone is increased. Due to the high temperatures which occur near the fusion line, grain-coarsening of this zone may result. For heavier sections and smaller welds, the hardening in the heat-affected zone may well exceed that commensurate with the retention of satisfactory ductility. Since the optimum ductility which may be expected even with the best metallurgical heat treatment is related (Fig. 4) to the hardness, often special techniques are resorted to, using preheat and postheat, in order to decrease the cooling rate and thus maintain the hardness at a low level so as to obtain a ductility approaching that of the original base metal.

From a metallurgical standpoint, the use of preheat and postheat can best be intelligently applied by remembering that the intent of any preheat is to control the postheat cycle, since hardening, with its loss in ductility, can only occur during the cooling cycle. The use of preheat and postheat is particularly effective in reducing the cooling rates at the lower temperatures. For example, it is expected that with a preheat of 300° F. which is maintained as an immediate postheat of 300° F., the rate of cooling in the 700 to 500° F. range will be reduced to a rate similar to that ordinarily obtained in the 400 to 200° F. range with no preheat or postheat. Such a reduction in cooling rate will result in transformation at a higher temperature, with the production of a more ductile structure in the heat-affected zone. It is well to remember that the maximum hardening under the most drastic cooling conditions is dependent upon the carbon content (Fig. 5). For example, in an S.A.E. 1050 steel (0.48% carbon, 0.51% manganese and 0.24% silicon), the maximum hardness attainable with the most drastic quenching is dependent upon the carbon content and would be 60 Rockwell C (700 Vickers).

Since the cooling rate determines the maximum hardness in the heat-affected zone of a given steel, it is natural to expect

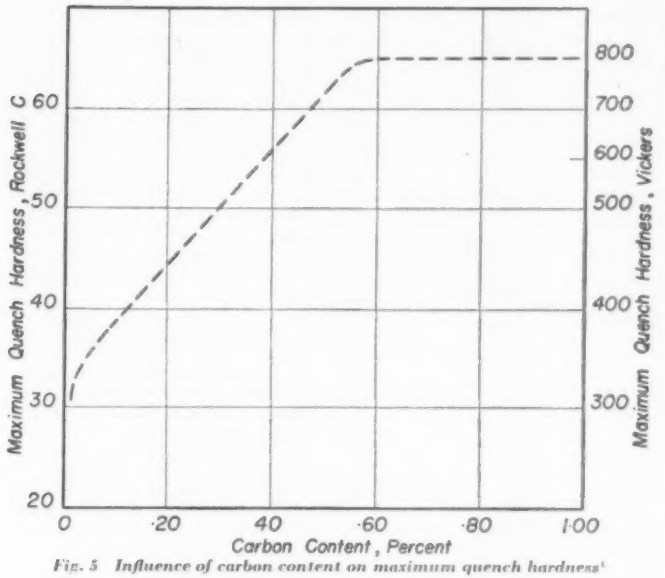

Fig. 5 Influence of carbon content on maximum quench hardness[4]

a decrease in hardness as the nugget area is increased. In order to determine the hardening effect of various sizes of bead welds on the sample of S.A.E. 1050 steel which was available, six surface bead welds were deposited by the Unionmelt process, using currents ranging from 260 to 725 amp., with 30 v., and 20 in. per min. travel. These currents were selected (Fig. 6) in order to give a wide range of nugget areas. The area of the weld nuggets ranged from 0.04 to 0.21 sq. in. The maximum hardness (Vickers 10 kg.) number was measured in the heat-affected zone for each bead weld, and is related to nugget area, as shown in Fig. 7. The smallest bead weld deposited with the lowest current produced the highest hardness in the heat-affected zone. The maximum hardness measured in the heat-affected zone decreased as the nugget size increased. Although increasing the size of the weld nugget does produce a lower maximum hardness in the heat-affected zone, often in welding hardenable steels it is desirable to use multiple-pass techniques in order to reduce the tendency for weld-metal cracking and excessive grain growth in the heat-affected zone.

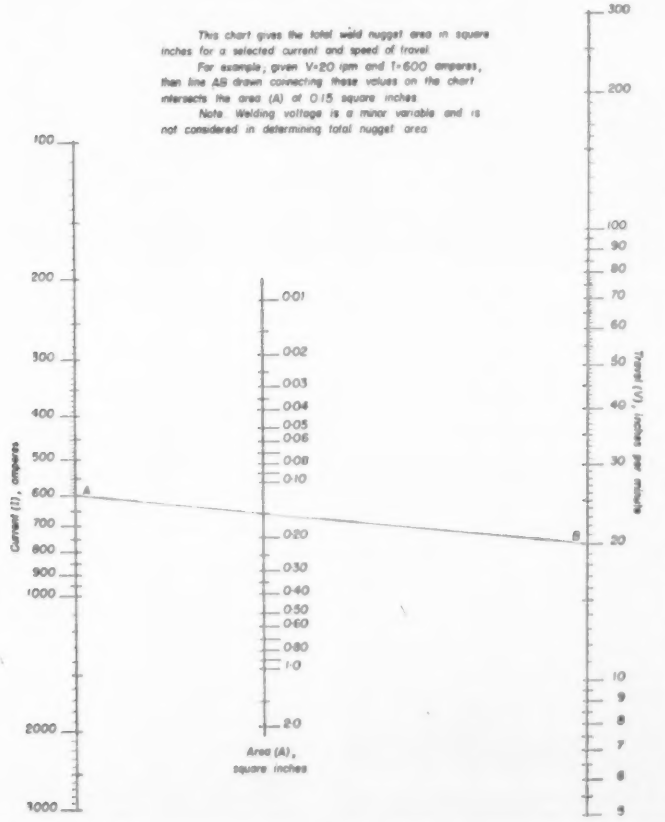

Fig. 6 Relation of welding technique to nugget area

CONTINUOUS COOLING

There is available in the metallurgical literature a vast fund of information regarding the rates of transformation of the metallurgical structures accompanying various cooling cycles. Most of these data deal with the progress of transformation of austenite at a given temperature, and diagrams showing the metallurgical changes with continuous cooling are not readily available.

The cooling conditions in the heat-affected zone of a weld (Fig. 2), however, are such that, at any point, cooling from the maximum attained temperature to the final temperature of the plate is continuous. Transformation diagrams with continuous cooling have been determined experimentally[7] for a few steels. Grange and Kiefer[8] have discussed the relationship of transformation on continuous cooling to transformation at constant temperature.

The progress of transformation of austenite on continuous cooling can be seen in Fig. 8 for a low carbon-manganese steel. In accordance with this diagram, if cooling in the heat-affected zone associated with a given size of weld nugget takes place in approximately two seconds or less, or at a rate fast enough to miss the nose at 950° F., the structure in the heat-affected zone will be martensitic and fully hardened. The structure in the heat-affected zone will transform at a higher temperature with slower cooling rates, such as those associated with a larger weld nugget.

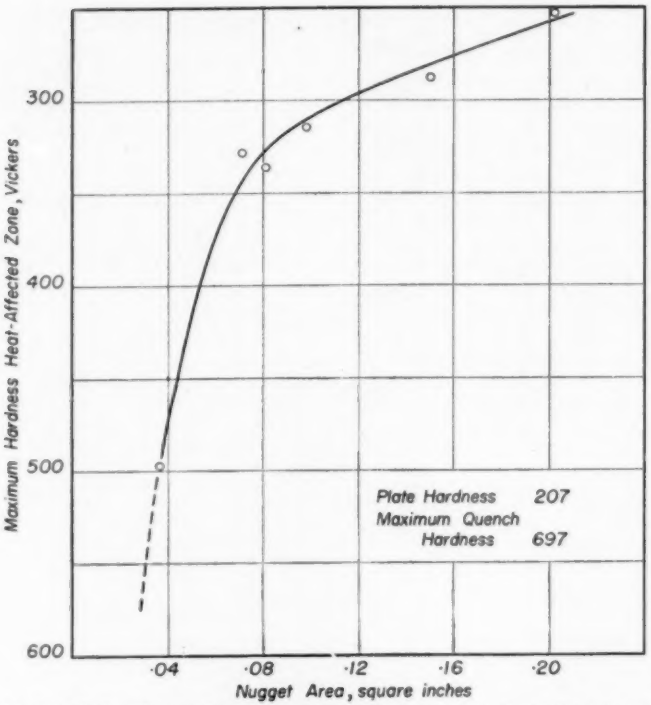

Fig. 7 Relation of nugget area to maximum hardness in the heat-affected zone for bead welds on ½-in. thick S.A.E. 1050 steel

The use of a relatively mild preheat of 150 to 400° F. will decrease the rate of cooling, and in this way, at least partial transformation may be obtained at the higher temperature. Wooding[8] has shown that only 8% transformation to the softer bainitic structures may be sufficient to prevent cracking in the heat-affected zone if the restraint is low. With higher restraint, it is felt that a larger percentage of the softer bainitic structure is needed for safety. It is regretted that a comprehensive, authoritative survey of continuous cooling transformation characteristics, of at least the standard grades of steels, is not available. Such a survey would be extremely useful in a rational approach to the effects of using mild preheats for welding. It should be remembered that the object of preheating and the use of immediate postheating are mainly to influence the cooling, so as to avoid the formation of martensite in such quantity as to give a hard heat-affected zone, with low ductility.

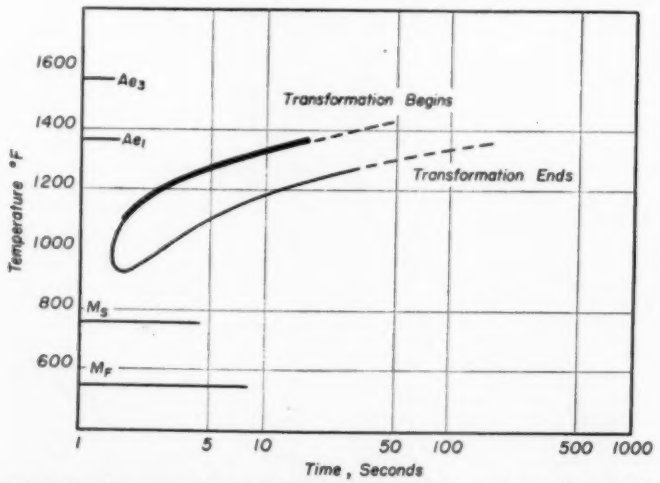

Fig. 8 Continuous cooling transformation diagram for steel containing 0.17% carbon, 1.21% manganese, 0.27% silicon, 0.005% vanadium, 0.008% titanium[7]

ISOTHERMAL COOLING

Although the information available for continuous cooling transformation is not sufficient to provide a wide knowledge of the transformational characteristics of the many steels in the welding field, we are fortunate in having available a vast number of diagrams which can be used. These diagrams[10] have been presented under various names, such as S-curves, time-temperature-transformation (T-T-T) diagrams or isothermal diagrams, and show the progress of transformation at any given temperature. They have been the subject of extensive studies.*[11]

In Fig. 9 is shown the isothermal diagram of an S.A.E. 1321 steel, containing 0.20% carbon and 1.88% manganese. This diagram was established by quenching a large number of small specimens from a temperature of 1700° F. into heated baths (500 to 1300° F.) and holding for varying lengths of time before quenching to room temperature. These specimens were then examined by measuring the hardness and observing the metallurgical microstructure. From such an examination, progress of transformation can be determined. The resulting isotherma[1] diagram summarizes the results of these tests and maps out the influence of temperature and time on the progress of transformation.

For our use, it is to be pointed out that in accordance with the diagram shown in Fig. 9, if the preheat and postheat employed are such that cooling to 800° F. is retarded to slightly less than 200 sec. (approximately 3 min.), the structure in the heat-affected zone will be completely transformed, giving ferrite and carbide (labeled F + C), with a hardness of 31 Rockwell C. This transformation is slower as the temperature of transformation is increased. As shown in the diagram (Fig. 9), the time for complete transformation at 1000° F. is over 10,000 sec., or about 2 ¾ hr. The resulting structure, however, is softer, having a hardness of only 17 Rockwell C, with the possibility of

* A very excellent atlas of isothermal transformation diagrams has been recently published by United States Steel; this has been freely consulted in gathering material for this paper.

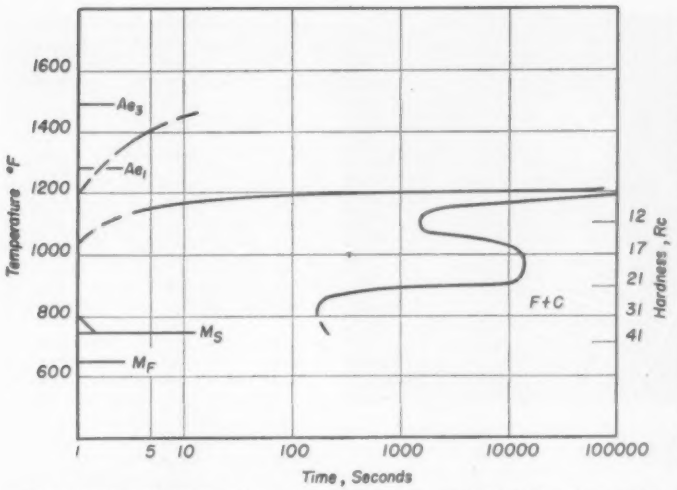

Fig. 9 Isothermal diagram for S.A.E. 1321 steel containing 0.20% carbon, 1.88% manganese (U. S. Steel, Reference 11)

example, will transform much of the structure in the heat-affected zone to martensite, with a hardness of 49 Rockwell C. In any transformational diagram, measurements of the hardness of the specimens used in making the diagram often are useful. For fully transformed specimens, the hardness increases as the transformational temperature decreases. For specimens with continuous cooling, the increase in hardness, with increased cooling rates, is also related to the transformational characteristics of the steel.

Much of the discussion so far has dealt with a comparison of the transformation of austenite with isothermal or continuous cooling. In welding with no preheat or postheat, the structures attained in the heat-affected zone are those which result from continuous cooling. However, in many steels the use of preheat or postheat, in order to ensure completion of transformation at a higher temperature with the accompanying lower hardness and improved ductility, is essential for the production of crack-free welds with the desired mechanical properties.

Fig. 10 Bead weld bend-test specimens on S.A.E. 1050 steel showing effect of postheat on ductility. Specimens from left to right welded with no preheat, with 400° F., 500° F., 600° F. and 800° F. Preheat, respectively.

Table for Fig. 10

Preheat, ° F.	Maximum Vickers hardness, HAZ	Angle at Maximum load, degrees
none	536	12
400	356	16
500	314	31
600	306	75
800	209	99

increased ductility. It is also to be noted that if the heat-affected zone is permitted to cool at a sufficiently rapid rate, a hard, martensitic structure will be formed at temperatures below the M_s temperature, which in this steel is approximately 750° F. Cooling to 600 F. at a rapid rate, for

EFFECT OF PREHEAT AND POSTHEAT

The effect of increasing preheat tem-

Table for Fig. 11

Time of postheat, minutes	Maximum Vickers hardness, HAZ	Angle at Maximum load, degrees
0	306	75
6	245	119
20	258	122

Fig. 11 Bead weld bend-test specimens on S.A.E. 1050 steel showing effect of preheating at 600° F. and postheating at 600° F. for 0, 6 and 20 min.

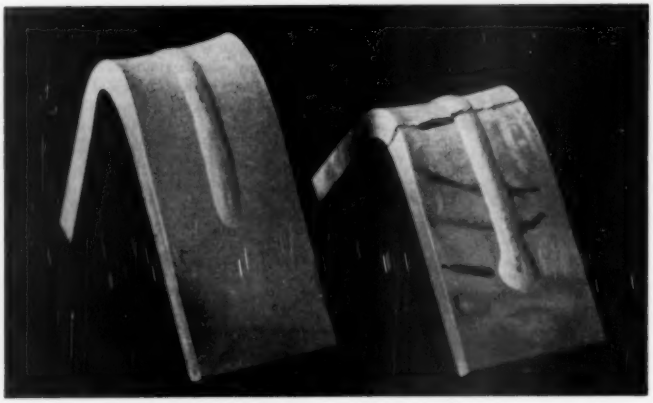

Fig. 12 Bead weld bend-test specimens on S. A.E. 1050 steel showing advantage of 600° F. postheat (shown on left) over 600° F. tempering heat treatment (shown on right)

Table for Fig. 12

Treatment	Maximum Vickers hardness, HAZ	Angle at maximum load, degrees
Postheated	258	122
Tempered	292	84

perature for Unionmelt welds on a 1/2-in. thick S.A.E. 1050 steel is shown in Fig. 10. The welds in these bend specimens were made using 400 amp. d. c.-r. p., 26 v. and 40 in. per min. travel, with "Oxweld" No. 40 electrode and Unionmelt composition 50-grade, 8- by 150-mesh. Preheating at 400° F. was not sufficient to produce good ductility; a gradual improvement in ductility was obtained with preheats of 500 to 800° F. In Fig. 11 are shown the effects of a preheat treatment at 600° F. with a postheat of 600° F. for only 6 min., transformation was almost complete, and the ductility was almost as good as a postheat treatment at 600° F. for 20 min. It is essential that

Table 1—Composition and Suggested Time and Temperature for Steels Shown in Figs. 14 and 15

S.A.E. Steel	Chemical composition						Time of postheat for complete transformation			Maximum quench hardness, Rc
	% C	% Mn	% Ni	% Cr	% Mo	% Other	Temp., ° F.	Time min.	Hardness, Rc	
1019	0.17	0.92	...	...	...		...	...	19	42
1050	0.50	0.91	...	...	...		600	10	48	62
							1000	1	28	
1335	0.35	1.85	...	...	...		700	10	39	59
							1000	20	18	
2160	0.61	0.27	0.99	...	...		600	10	48	65
							1000	1	24	
2260	0.56	0.26	1.97	...	...		600	25	50	64
							1000	1	25	
2340	0.37	0.68	3.41	..	...		600	35	46	60
							1000	5	21	
2512	0.10	0.52	5.00	...	...		700	5	36	42
							900	75	20	
3140	0.38	0.72	1.32	0.49	...		700	25	48	60
							1000	15	21	
3330	0.29	0.21	3.25	1.45	...		600	25	43	57
							800	20	38	
							1100	10 hr.	23	
4037	0.35	0.80	...	...	0.25		700	10	41	56
							900	5	25	
4130	0.33	0.53	...	0.90	0.18		700	10	44	56
							800	5	37	
4340	0.42	0.78	1.79	0.80	0.33		600	50 min.	48	62
							700	5 hr.	40	
4360	0.62	0.64	1.79	0.60	0.32		500	10 hr.	54	64
							650	50 hr.	48	
							1200	75 min.	26	
4615	0.15	0.63	1.90	...	0.24		700	10	38	45
							900	10	26	
4640	0.36	0.63	1.84	...	0.23	...	600	25	48	60
							900	25	23	
5140	0.42	0.68	...	0.93	...	...	600	15	50	62
							1000	25	26	
6145	0.43	0.74	...	0.92	...	0.16 V	600	25	51	61
							1000	25	33	
8630	0.30	0.80	0.54	0.55	0.21	...	600	15	46	53
							900	25	27	
8660	0.59	0.89	0.53	0.64	0.22	...	500	5 hr.	52	64
							800	50 hr.	34	
8745	0.44	0.90	0.45	0.54	0.22	...	600	25	49	61
							800	100	30	
9260	0.62	0.82	...	0.07	...	2.01 Si	600	100 hr.	50	65
							1200	1 min.	35	
9440	0.38	1.08	0.34	0.40	0.11	0.030 Zr	600	50 min.	50	60
						0.70 Si	800	10 hr.	33	
10B60	0.63	0.87	...	...	...	0.0018 B	600	25	50	65
							1000	1	36	
86B20	0.18	0.83	0.49	0.49	0.19	0.0013 B	700	5	40	45
							900	15	26	

Fig. 13 Bead weld bend-test specimens on S.A.E. 1050 steel showing slight improvement in ductility with preheat and postheat of 400 and 500° F. Pair at left treated at 400° F. Pair at right treated at 500° F. Second specimen in each set postheated after welding at temperature for 30 min.

Table for Fig. 13

Preheat, ° F.	Postheat time, min.	Maximum Vickers hardness, HAZ	Angle at maximum load, degrees
400	..	354	16
400	30	351	17
500	..	314	31
500	30	325	29

the postheat treatment is run immediately following welding, since cooling the specimen to room temperature and then reheating to 600° F. for 20 min., normally referred to as a tempering treatment, is not effective in improving the ductility (Fig. 12). Postheat treatment at 400 and 500° F. at temperatures below the M_s (start of martensite transformation) temperature was not effective in improving the ductility (Fig. 13).

Figures 14 and 15 show diagrams which indicate time-temperature relationships for complete transformation for a range of compositions of steel given in Table 1. These diagrams may be used as a guide for postheat treatment for improved ductility in the heat-affected zone. In welding, maximum ductility in the heat-affected zone will be obtained by using sufficient preheat or postheat to ensure complete transformation at a temperature sufficiently high to limit the hardness after transformation to a level which provides the desired ductility. For steels such as S.A.E. 4130 and 3140 with rapid transformation characteristics, a mild preheat may be sufficient to maintain the temperature at a high enough level so that transformation is completed without the use of additional postheat to retard the cooling. This will be particularly true for thinner sections. For steels such as S.A.E. 4340 and 8660 and in heavier sections, the preheat temperature will not be sufficient to maintain the postheat temperature long enough to obtain transformation at the desired level. A postheat treatment immediately following the welding operation will permit the transformation to take place at the desired temperature during the cooling cycle.

Recent metallurgical developments have emphasized the addition of boron to the leaner medium-alloy steels in order to improve their hardenability. Since the maximum hardness in the heat-affected zone with its related ductility is a function of the hardenability of the steel, the welding of these steels has raised a number of questions. It is fortunate, however, that from a welding standpoint, two factors have simplified the handling of these steels. First, previous tests have indicated that the hardenability of boron-bearing steels may decrease as the temperature of treatment is increased. In a like manner the high temperatures in the heat-affected zone accompanying welding will tend to reduce the hardenability in this zone. Second, as shown in Fig. 15, steels S.A.E. 86B20 and S.A.E. 10B60 compared with similar steels without boron, show little effect of boron on the time-temperature for completion of transformation. Consequently, it is to be expected that the use of preheat and postheat in welding a boron-bearing steel will be similar to that for a steel of like composition without boron.

TYPICAL APPLICATIONS

In the following sections are presented a number of actual applications of controlled preheat and postheat treatments in submerged-arc welding of alloy steels.

In one application it was desired to build up two 7 $^3/_4$-in. diameter solid rolls, 24 in. in length. These rolls were made of S.A.E. 4145 and 4340 steels. The rolls were to be built up 2 in. in thickness on the radius, using appropriate welding conditions with S.A.E. 4150 and 4350 composition electrodes. As can be seen from Fig. 14, transformation isothermally for steels S.A.E. 4130 and 4340 is most rapid at a temperature range of 500 to 700° F. The desired build-up was obtained by using the following technique:

S.A.E. Steel No.		Weld-Metal hardness	Welding conditions		
Roll	Electrode	(Vickers)	Amp.	Volts	Speed, ipm
4145	4150	260	310	30	12
4340	4350	260	310	30	12

Unionmelt composition	80-grade, 12- x 200-mesh
Welding power	Direct current-reverse polarity
Welding electrode	$^5/_{32}$ in. diameter
Rod angle	30-degree backward
Electrode off-center	$1^1/_8$ in.
Preheat and interpass temperature	500 to 700° F. (Fig. 14)
Postheat temperature	500 to 700° F. maintained for 20 min. with slow cool

In a second application, it was desired to determine the effect of preheat and postheat on $^1/_2$-in. thick S.A.E. 4130 steel. The following welds were made:

	Welding conditions				
Weld no.	Amp.	Volts	Speed, ipm.	Preheat, ° F.	Postheat, ° F.
17250	850	28	16	None	None
17249	850	28	16	650	650 (5 min.) } Fig. 14
17290	850	28	16	850	850 (2 min.) }

Unionmelt composition	80-grade, 20- by 200-mesh
Bevel preparation	60-degree total angle $^5/_{16}$-in. nose
Backing	$^1/_2$- by 4-in. copper bar
Welding power	Alternating current
Welding electrode	Oxweld No. 40, $^1/_4$ in. diameter

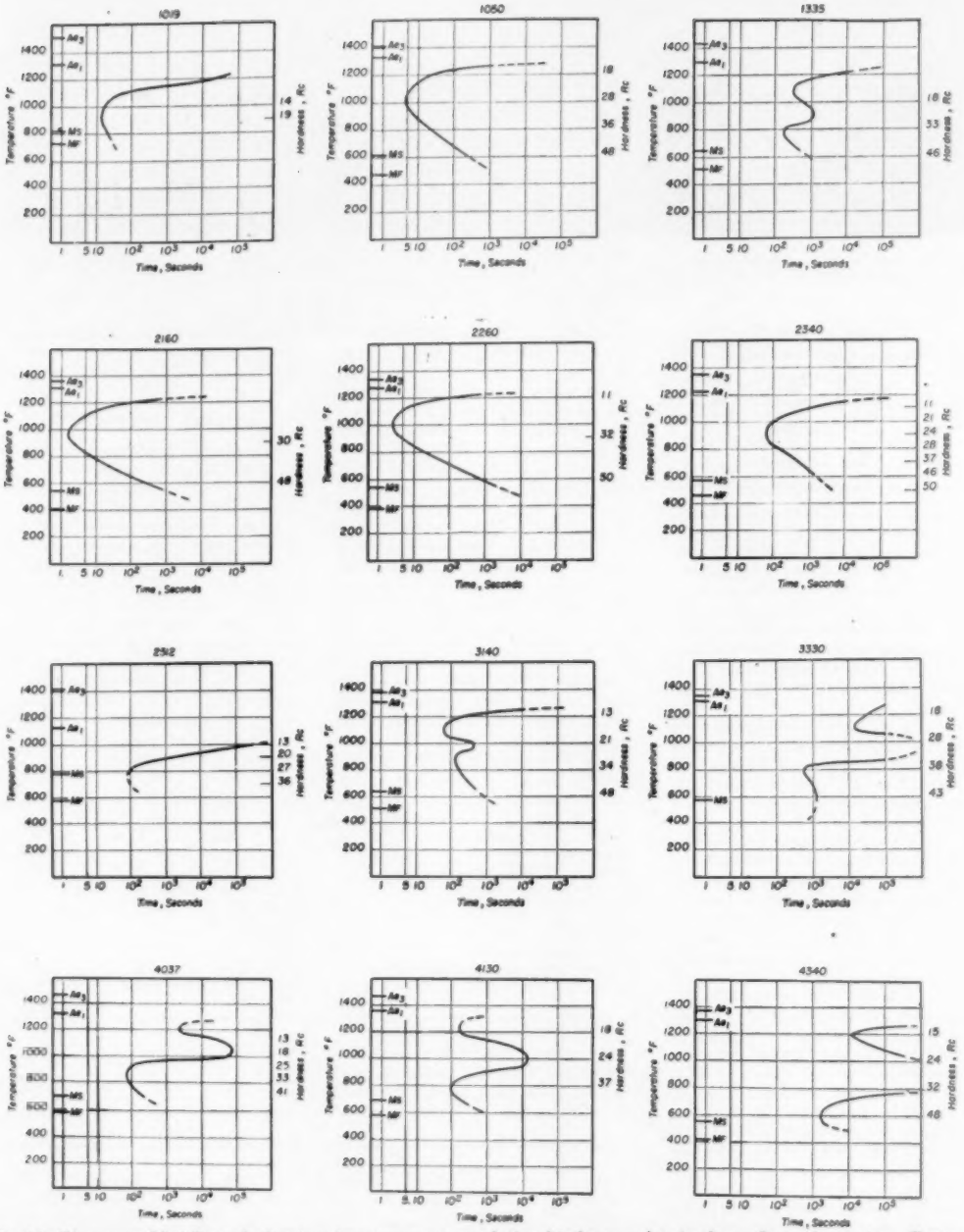

Fig. 14 Diagrams of S.A.E. steels showing time-temperature relationship for complete isothermal transformation. Transformation is completed at time and temperatures to the right of the curve shown. See Table 1 for chemical composition and suggested time and temperature for postheat

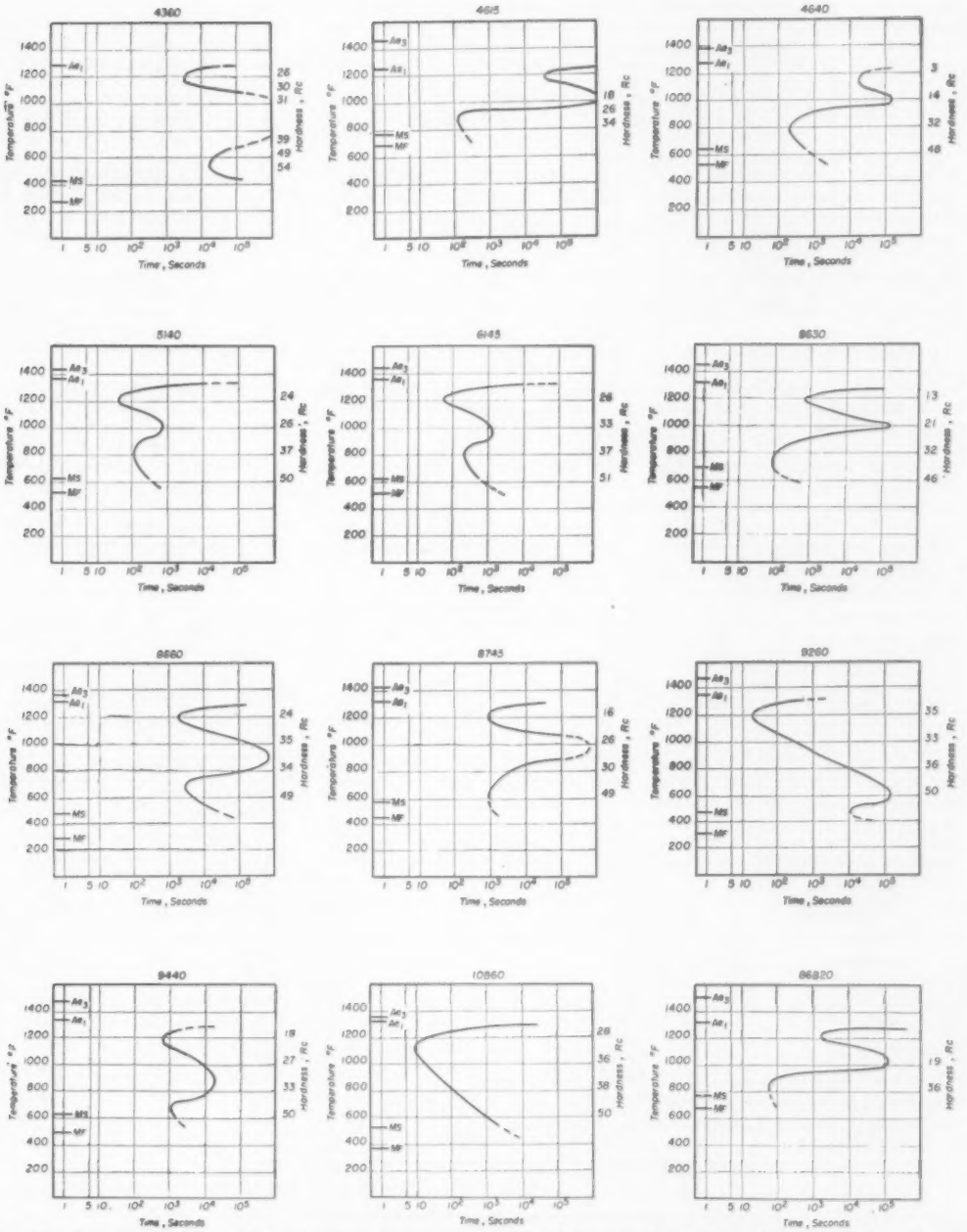

Fig. 15 Diagrams of S.A.E. steels showing time-temperature relationship for complete isothermal transformation. Transformation is completed at time and temperatures to the right of the curve shown. See Table 1 for chemical composition and suggested time and temperature for postheat

The hardnesses, measured in the heat-affected zones and weld metal on cross sections from these welds, were as follows:

Weld no.	Maximum HAZ hardness — Rockwell C	Vickers	Weld-metal hardness — Rockwell C	Vickers
17250	35	350	30	300
17249	27	280	25	265
17290	21	245	18	230

From Fig. 14, the hardnesses in the heat-affected zones for Welds Nos. 17249 and 17290 were slightly lower than would have been obtained from a true isothermal transformation at the preheats and postheats employed. This is expected, since the plates were cooled slowly after the postheat treatment and undoubtedly transformed at a temperature above the postheat temperature.

In another application it was desired to butt-weld an S.A.E. 1060 steel tubing $1^3/_{16}$ in. thick and 15 in. in diameter in the fabrication of drying rolls. From Fig. 14 in the diagram for S.A.E. 1050 steel, a minimum preheat of 550° F. is indicated. Sound welds were made in this tubing, using the following technique:

Weld no.	Side	Electrode	Welding conditions — Amp.	Volts	Speed, ipm.
15505	Inside	Oxweld No. 36	900	31	5
15506	Outside	Oxweld No. 296	950	31	5

Unionmelt composition: 90-grade, 20- by D-mesh
Bevel preparation: Inside: 90-degree total angle, $^3/_{16}$ in. deep, $^1/_{16}$ in. root face; Outside: 100-degree total angle, $^9/_{16}$ in. deep
Backing: Special copper bar
Welding power: Alternating current
Electrode diameter: $^1/_8$-in.
Preheat temperature: 550° F. (Fig. 14)
Postheat temperature: 550° F. 1 hr. minimum

In a fourth application, tubes of S.A.E. 4340 steel, 5 $^3/_8$ in. I.D. by 7 in. O.D., were butt welded. From Fig. 14, a postheat treatment at 600 to 700° F. appeared logical from the diagram for S.A.E. 4340 steel. The desired butt welds were obtained by using the following multipass welding technique:

Passes	Welding conditions — Amp.	Volts	Speed, ipm.	Offset, in.
1 and 2	350	30	9	$^7/_8$ and 1
3, 4 and 5	375	30–32	9	$1^1/_8$
6, 7 and 8	350	30	9	$1^1/_8$

Unionmelt composition: 80-grade, 12- by 65-mesh
Bevel preparation: U-groove, $^5/_{16}$-in. radius, $^1/_{16}$-in. nose, 20-degree total angle
Backing rings: $5^3/_8$ in. O.D. by $^3/_8$ in. thickness
Welding power: Alternating current
Welding electrode: $^5/_{16}$-in. Oxweld No. 502
Electrode off-center: Nos. 1 and 2, $^7/_8$ and 1 in.; Nos. 3 to 8, $1^1/_8$ in.
Preheat and interpass temperature: 650 to 700° F. (Fig. 14)
Postheat temperature: 650 to 700° F. 1 hr. minimum

SUMMARY

These examples illustrate the use of preheat and immediate postheat for producing weld joints with improved ductility. Essentially, the diagrams in Figs. 14 and 15 are of sufficient similarity to serve as a guide in setting a technique for an unknown steel. In order to establish the minimum time which might be used with any steel, a series of specimens, such as shown in Figs. 10 to 13, would be useful. Maintaining the postheat for a longer period than necessary for transformation is actually metallurgically beneficial. In the welding of medium-alloy steels, the pieces are sometimes preheated in a furnace, welded with this preheat maintained, and then returned immediately to the furnace and held for a time far in excess of the minimum requirement.

In summary, in the welding of medium-alloy steels, it is essential to again point out that:

1. Maximum quenched hardness is dependent upon the carbon content (Fig. 5).

2. The area for complete transformation is shifted to the right toward longer times with increased carbon content (See Figs. 14, S.A.E. 1019 and 1050) or increased alloy content (See Fig. 14, S.A.E. 4130 and 4340).

3. The postheat temperature required to maintain a given level of hardness in the heat-affected zone generally increases with carbon content. (See S.A.E. 1019 and 1050, Fig. 14.)

4. Welding electrodes with compositions and properties suitable for the intended service must be selected. Often a more simple composition or one matching the composition of the base metal may be used, depending upon whether the final structure is subjected to further heat treatment.

5. Postheat treatment is most advantageous in welding those steels which are sufficiently sluggish in their transformation, so that a simple preheat practice results in low ductility in the heat-affected zone.

6. The newer boron-bearing steels show little effect of the boron on the time-temperature for completion of transformation.

7. The diagrams which are presented have been developed generally from standard heat-treatment practices. The high temperatures and shorter time-temperature cycles, accompanying welding, will modify the transformation somewhat.

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Creep and Stress-to-Rupture Properties of Pressure Vessel Steels

An interpretive engineering statement of present knowledge on the creep and stress-to-rupture characteristics of pressure vessel steels

by R. D. Wylie and H. Thielsch

Summary

In the design of pressure vessels for service applications at elevated temperatures, various factors may impose limitations on the service life. Thus, such factors as creep and stress-rupture, fatigue, surface oxidation, structural changes within the material, corrosion and others have to be considered in the selection of materials and in the design of pressure vessels. Among these, creep and stress-rupture represent two of the more important factors. Creep is generally defined as the time-dependent part of the deformation which accompanies the application of a constant load to a solid. Usually it is expressed as a deformation rate, e.g., 1% in 100,000 hr. Stress-rupture, on the other hand, is the time required for fracture under a constant load or, more often, the stress required to produce fracture in a specified time.

Since this discussion is to serve primarily as an interpretive engineering statement of the latest knowledge on the creep and stress-to-rupture characteristics of pressure vessel materials, detailed research results as well as theoretical hypotheses have been omitted.

INTRODUCTION

WHEN a load is applied to a metal at an elevated temperature over a prolonged period of time, the metal may undergo continuous plastic deformation, that is, it may experience a progressive change in its dimensions. The amount of gradual deformation or "flow" depends on the composition, processing and heat treatment of the material, the temperature, the shape of the section and the initially applied stress. At certain temperature levels creep, which is the term used to describe this progressive dimensional change, may occur in metals even at stresses below the short-time yield strength or proportional limit. Thus the yield strength and proportional limit, which are determined by short-time tensile tests at room or at elevated temperatures, do not represent satisfactory criteria for the design of pressure vessel equipment over the entire temperature range. Even for those cases where total deformation is considered relatively unimportant, data obtained from short-time elevated-temperature tensile tests must still be augmented by long-time sustained load tests since creep at elevated temperatures may terminate in fracture even at loads considerably less than the short-time tensile strength. Such high-temperature fractures are commonly referred to as stress-rupture failures, and the long-time tests, generally under constant load, carried out to fracture are called stress-to-rupture tests.

Since short-time elevated-temperature tensile tests do not furnish data which are suitable to the design engineer for high-temperature applications, a great many long-time tensile tests have had to be and are being made on various materials at elevated temperatures to determine the rate of deformation or creep rate, or the fracture time, as a function of applied stress and temperature. It may seem most desirable to be able to determine so-called "creep limits," which, for each material, would represent the stress at a particular temperature below which plastic deformation or creep either would not occur or would occur at too slow a rate to be significant. Such a limit, however, is merely a practical assumption, not a true value, since creep may be found to occur at increasingly lower temperatures and stresses as the accuracy and sensitivity of the testing equipment improve.

Since a definite creep limit does not exist, engineers must take into consideration some degree of plastic yielding during service and design in accordance with such maximum applied stresses as will result in no more than certain specific limiting deformations during the contemplated service life of the vessel. The amount of permissible deformation and the contemplated service life may vary for different applications. In most pressure vessels as, for example, those built to ASME Boiler Code rules, the stress to produce a creep rate of 1.0% elongation in 100,000 hr. determined from tests lasting only a few thousand

R. D. Wylie is Metallurgical Engineer, Works Laboratory, Babcock & Wilcox Co., Barberton, Ohio, and Helmut Thielsch is Technical Assistant, Pressure Vessel Research Committee, New York, N. Y.

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hours, has been considered a limiting value; however, this is generally considered a practical limit based on experience and is not necessarily the maximum deformation the vessel could satisfactorily sustain. In the design of mechanical equipment, such as turbines and pumps, required clearances might necessitate the use of lower stresses to limit the creep to elongations of less than 1.0% in the desired life time. Where large deformations can be tolerated, as for example in still tubes, the results of stress-to-rupture tests may have a greater significance than those of creep tests.

Thus, in the design of vessels, it is necessary to know the effects of the three independent variables: stress, time and temperature on the plastic properties and fracture strength of the materials from which the vessel is to be constructed. Such information is obtained from creep tests. Since creep rates, as determined by creep tests, will not indicate the order of elongation with which a material will fail, it is also desirable to know the stresses which will result in rupture as a function of time and temperature. This information is obtained from stress-to-rupture tests and has been used together with creep and short-time tensile-test data by the ASME Boiler Code Committee, the American Petroleum Institute and by similar organizations abroad to establish design stresses for the materials which are used in pressure vessels intended for service at elevated temperatures.

TEST DATA

One of the major problems in the design of pressure vessels intended for long service (such as 10, 15 or 25 years) at elevated temperatures is that design values must be based upon extrapolation of relatively short-time laboratory test data.

In addition to the determination of the relation between deformation and time (the creep rate), it is also important to know the total amount of deformation which the material can experience before it ruptures. Otherwise, the vessel, which is designed on the basis of extrapolated creep data, may fail because the particular material ruptured before attaining the contemplated (permissible design) strain. Thus, the engineer should be interested in stress-to-rupture data as well as in creep data.

The creep and stress-to-rupture tests are very similar. In each test, a tensile specimen is loaded in uniform axial tension at a constant temperature. However, whereas in the creep test the changes in length (elongation) are recorded, in the stress-to-rupture test the time required to cause failure is of primary interest. In order to cause rupture in practical testing periods (up to 10,000 hr.), higher applied stresses are usually used in the stress-to-rupture test than in the creep test. In the more modern machines both test criteria are usually measured so that determination of changes in length (strain rates) are made during the stress-to-rupture test.

Creep Data

The conventional creep test represents a precise measurement of the deformation of a tensile specimen exposed under a constant load at a particular elevated temperature. The tests are performed with very close temperature control and are usually run for times of from 1000 hr. to 10,000 or 20,000 hr. The elongation is read at more or less regular time intervals.

The results of typical creep tests for several different loads (or, instead, for different temperatures) are shown in Fig. 1. The curves may be divided into four sections designated by *OA*, *AB*, *BC* and *CD*.

1. *OA* represents the elongation which occurs as soon as the load is applied. It consists partly of an

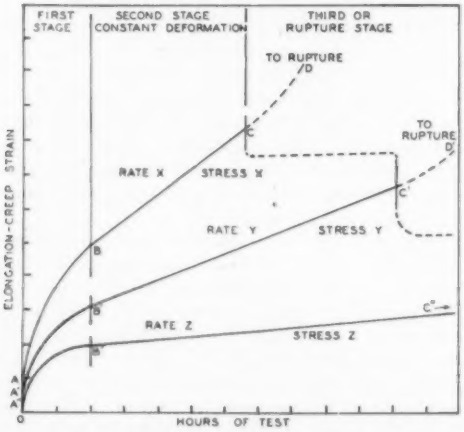

Fig. 1 Creep time vs. elongation curves at a given temperature

(For many steels the duration of the third stage is equal for that of the first and second stages combined.)

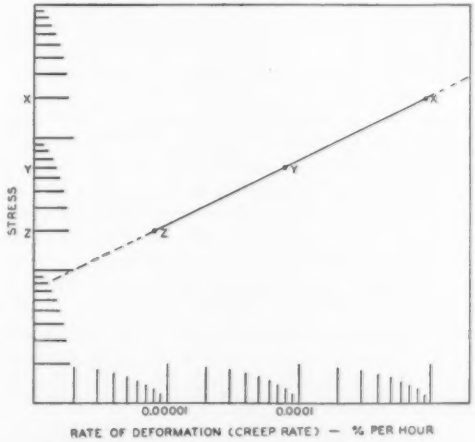

Fig. 2 Stress vs. deformation-rate curve

elastic strain (recoverable) and partly of a plastic strain (not recoverable). Generally this stage (*OA*) is referred to as a rapid straining.

2. *AB*, also called *first-stage creep*, is a period characterized by a decreasing creep rate.

3. *BC*, or *second-stage creep*, is characterized by a relatively constant creep rate which also represents the minimum rate.

4. *CD*, or *third-stage creep*, corresponds to an increasing creep rate which ends in the rupture of the specimen.

The logarithm of the deformation rate, in the second stage of creep (*BC*), frequently expressed in per cent elongation per hour, usually is either a natural or logarithmic function of the applied stress at a given temperature. The latter relationship is illustrated in Fig. 2. In plotting this curve, it is well to have data for at least three loads (stress levels) at a given temperature, so that it will be possible to interpolate or extrapolate with some degree of assurance. A plot such as this is used to determine the normally accepted design criteria as, for example, the stress *Y* to produce a second-stage (constant) creep rate of 1% in 10,000 hr. ($1^1/_8$ year), corresponding to a creep rate of 0.0001% per hour or the stress *Z* to produce a creep of 1% in 100,000 hr. ($11^1/_4$ years), equivalent to a creep rate of 0.00001% per hour. These respective points are indicated in Fig. 2. The usual custom of considering a creep rate of 0.0001% per hour as equivalent to one of 1% in 10,000 hr. or of 0.00001% per hour as equivalent to one of 1% in 100,000 hr. is an assumption which may or may not be entirely valid.

The third stage of creep (*CD*) is characterized by an increase in the rate of elongation. This stage of creep is caused by the reduction in cross-sectional area or by a damaging structural change or by intergranular oxidation. Once the material has entered this stage it is not possible to restore its original properties by further mechanical or thermal treatment. Consequently, creep rates of this stage are never used in the design of pressure vessels. Thus, it is important for the designer to know that, at the design stress, the material will not enter the third stage of creep before the expected service life of the vessel has been completed.

By plotting, on logarithmic paper as in Fig. 3, the applied stress versus the time at which the specimen enters the third stage of creep, the maximum stress at which the specimen will still exhibit a constant rate of creep is obtained for given testing times. Since the change from second- to third-stage creep generally is not abrupt, this curve represents an empirical interpretation of the maximum second-stage creep stress. Data of this type often are not as useful to the design engineer as the information obtained from stress-to-rupture tests.

Stress-to-Rupture Data*

Stress-to-rupture data are obtained from high-temperature, long-time tensile tests in which the specimen is tested to rupture. Higher stresses (or loads) usually are applied in stress-to-rupture tests than in creep tests in order to obtain rupture in reasonable times of the order of 100 to 5000 hr. However, many tests have been conducted much longer and the same time range could be used as in the creep test.

In order to obtain a straight-line relationship, the stress is generally plotted against the time for rupture at a constant temperature on log-log coordinates as illustrated in Fig. 4. For many materials, a sudden change in slope (Curve *C*) often is observed in the stress-to-rupture curves. This change is sometimes associated with a change in the type of fracture from transgranular at relatively short fracture times to

* The term creep-to-rupture, although used less, is technically more accurate. Some authors use this term to denote a continuation of the creep test to rupture.

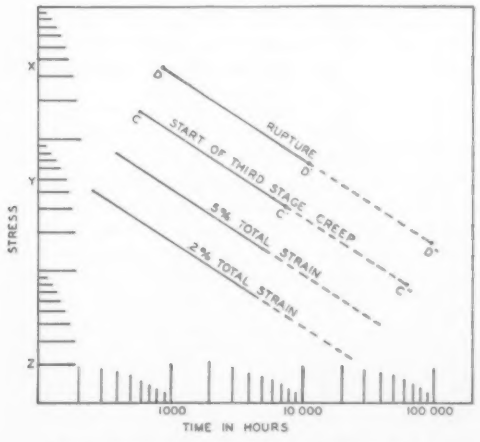

Fig. 3 Stress vs. initial third-stage creep curve

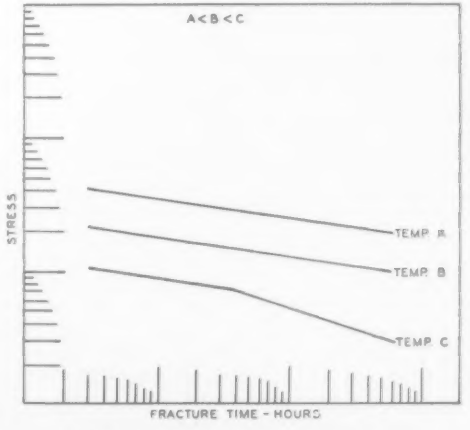

Fig. 4 Stress vs. rupture-time curve

intergranular at longer times. However, steels that do not show a break in the slope of the curve may also fail intergranularly in the more prolonged tests. The transition from transgranular to intergranular fracture is not abrupt, and mixed fractures occur at intermediate rupture times. Since intergranular fractures generally start at the surface of the specimen, they are considerably affected by the stability of the material with respect to the atmosphere which surrounds the specimen. Other types of phenomena such as sigma phase formation in certain stainless steels, rates of strain hardening, recrystallization, and spheroidization and tempering in quenched or normalized steels may also cause a break in the rupture curve.

Creep and stress-to-rupture data, expressed as stress to produce a specific creep rate or rupture time, are sometimes replotted on natural coordinate or semi-logarithmic coordinate paper as stress, or log-stress, for a constant strain rate versus test temperature, as shown in Fig. 5. This procedure allows extrapolation or interpolation between the test data for various temperatures. Generally, however, the log-log curves for constant test temperatures (Fig. 4) are preferred.

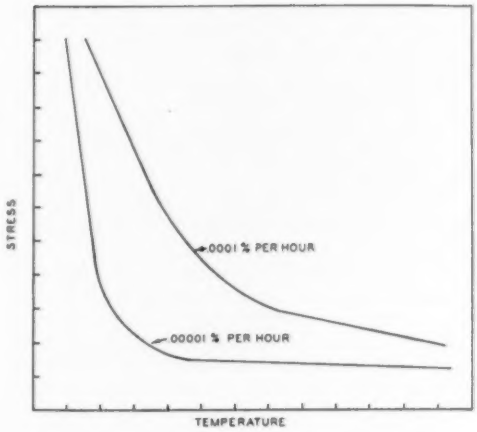

Fig 5 Creep stress vs. temperature curves

MISCELLANEOUS FACTORS AFFECTING CREEP STRENGTH

The creep and stress-to-rupture properties are considerably influenced by the metallurgical characteristics of the material and the testing and service environments to which it is exposed.

The most important metallurgical characteristics of the material are (1) chemical composition, (2) structure and grain size which are primarily controlled by the prior processing and heat treatment and (3) structural stability at testing or service temperature. Close control of many of the metallurgical factors is not always possible, since some of them are dependent upon the chemical composition, the steelmaking practice, the prior heat treatment and fabrication processes.

Effects of Composition

Composition is the most obvious and important variable which affects the high-temperature strength of steels. Improvement in creep and stress-to-rupture properties by alloying additions generally may be related either to the amount and size of fine particles which are distributed within the structure of the steel or to a general strengthening effect of the over-all (matrix) structure without the formation of such particles. However, the mere addition of alloying elements does not insure higher creep and stress-to-rupture properties. Thus, some elements may reduce these properties. Other elements which may act in a highly beneficial manner in certain quantities may be detrimental in other quantities. The correct usage of alloying elements by the steel producer is highly important when, with careful quality control and further research, he strives to improve present grades of steel and to develop new grades. Typical creep and stress-to-rupture curves for a carbon steel, two low-alloy steels and an 18-8 (Cr-Ni) stainless steel are shown in Figs. 6 and 7, respectively. The curves show how the elevated-temperature strength of steel may be effectively increased by addition of molybdenum or by highly alloying as with the stainless alloy.

Effects of Structure and Grain Size

Definite values of creep and stress-to-rupture properties cannot be accurately predicted from composition alone. This is because these properties are also structure sensitive.

At temperatures above the so-called equicohesive temperature* which varies for different steels (usually between 800 and 1000° F. for low-alloy steels) coarse-grained steels generally exhibit better creep and stress-to-rupture properties than fine-grained steels. Below the equicohesive temperature the trend reverses and steels with a fine grain size usually exhibit superior creep and stress-to-rupture properties.

Effects of Heat Treatment

Heat treatments are important primarily because of their effects on the structure of the steel. For example, heat-treating temperatures and techniques which produce a larger grain size generally will improve the creep and stress-to-rupture properties of steels at the more elevated temperatures. Cooling

* The equicohesive temperature is the temperature at which the grain strength (strength of the material within the grains) is equal to the strength of the material at the grain boundaries. Above the equicohesive temperature the grain strength is higher and below the equicohesive temperature the grain strength is lower than the grain boundary strength. Thus, above the equicohesive temperature failure will be intergranular and below the equicohesive temperature failure will be transgranular. The equicohesive temperature is affected differently by various factors. For example, increases in the rate of straining will raise the equicohesive temperature. Thus, this term, although being of considerable convenience, describes only a relative temperature depending upon the specimen material and testing conditions.

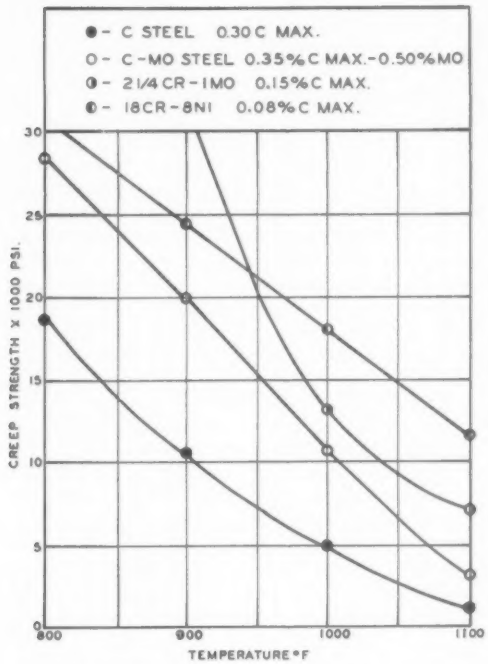

Fig. 6 Effect of alloy additions on creep strength. (Stress for creep rate of 1% per 10,000 hours)

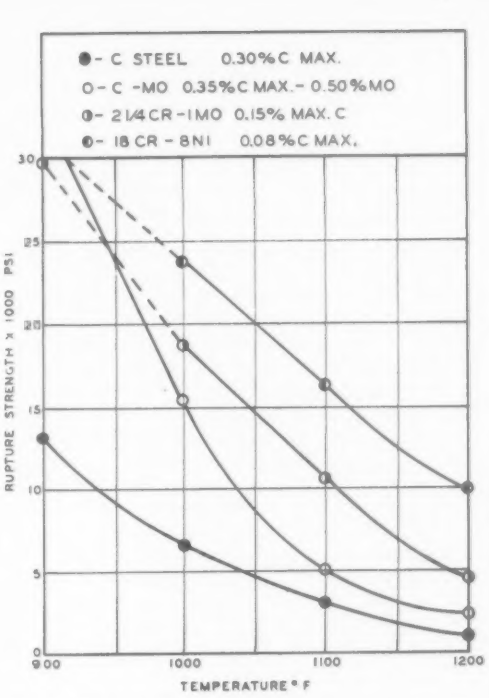

Fig. 7 Effect of alloy additions on the rupture strength. (Rupture time of 10,000 hours)

rates from the temperatures of the heat treatment are of similar importance. Other factors such as distribution of the structural constituents, as carbides, also are important as they are affected by heat-treating temperatures and cooling rates. Thus, a normalized and tempered steel is often superior to the same steel in the fully annealed condition. When heat-treated steels are employed at elevated temperatures it is customary to use a tempering temperature at least 150° F. above the expected service temperature.

Effects of Testing Variables

Among the testing variables, it is most important that the testing temperature be kept constant. Short durations at higher temperatures may considerably reduce the creep and stress-to-rupture properties.

Stress-to-rupture and creep tests lasting 5000 hr. or longer are more significant than shorter tests because the longer tests minimize the errors introduced by the extrapolations necessary for practical use of the data. Thus creep or stress-to-rupture data can be considerably in error when extrapolated to 100,000 hr. from relatively short-time tests, for example tests of 1000 hr. or less.

Some of the variables, which can affect the results of tests obtained in different laboratories, are the mechanical operation of the test equipment and the design of the test specimen. The precision of temperature control, the precision of stress application and the precision of the strain-measuring equipment have an important effect on the final test results. A less understood, but probably important factor, also may be the variation in the dimensions of the creep and rupture specimens used in different laboratories. Reference should be made to A.S.T.M. Specification [E 22 for recommended testing procedures.

After the engineer and metallurgist are assured that the data on hand meet the qualifications necessary for the job—that is, the right material at the right temperature for a long enough duration of time to give a certain degree of assurance to extrapolation—a survey should then be made of the intended application for possible sources of trouble. Some of the important variables in operation are temperature cycling, stress cycling, corrosive atmospheres and thermal stresses over and above the major applied stress.

It is well to consider that most creep and stress-to-rupture tests are performed at a constant temperature, and that cyclic temperatures may materially reduce the strength of the metal when in service, because of the additional thermal stresses introduced which may shorten the expected life of the vessel. Possibly, some test data may, in the future, serve as a guide in establishing safety factors for applications in which materials are exposed to specific operating conditions,

although thermal stresses of the same order of magnitude and distribution as are encountered in service are difficult, if not impossible, to reproduce in the laboratory.

APPLICATION OF CREEP DATA TO THE DESIGN OF PRESSURE VESSELS

For most pressure vessel design engineers, the decision on what allowable stress value to use for a given material at a given temperature has been solved by the Construction Code under which the vessel is to be built. In the United States, the values for design stresses for steels are established by the *Subcommittee on Stress Allowances for Ferrous Materials* of the ASME Boiler Code Committee. This Subcommittee studied all of the available data and established tables of maximum allowable design stress values.

In the determination of stress values, the ASME Subcommittee on Stress Allowances for Ferrous Materials was guided by successful experience in service with the particular steels in question in so far as evidence of satisfactory performance is available, such evidence being preferred to all forms of test data. However, in the evaluation of new materials it was always necessary to be guided, to a certain extent, by the comparison of test information with similar data on established materials, and in carrying out this process on examination and evaluation the Subcommittee found the considerations as given in the following paragraphs useful.

At temperatures below the creep range, allowable stress values were established at the lowest value of stress obtained from using 25% of the specified minimum ultimate strength at room temperature or 25% of the minimum expected ultimate strength at temperature, or 62½% of the minimum expected yield strength for 0.2% offset, at temperature. For bolting material the stress values were based on 20% of the minimum tensile strength or 25% of the yield strength for 0.2% offset, whichever is lower. (It is recognized that bolts are always expected to work at stresses above the design value as distinguished from other parts.)

No credit was allowed for any improvement in tensile properties by special heat treatment.

At higher temperatures, where creep governs, the stress values were based on 100% of the stress to produce a creep rate of 0.01% per 1000 hr, the values so chosen being based on a conservative average of many reported tests as evaluated by the Subcommittee, greater weight being given to longer time tests in evaluating data. In addition to the above-stated

Table 1—Maximum Allowable Stress Values in Tension for Carbon and Low-Alloy Steel Plates in Pounds per Square Inch*

Material	*Specification No.*	*Grade*	*Notes*	*Spec. Min. Tensile Strength*	*For metal temperatures not exceeding (° F.):*						
					−20 to 650	*700*	*750*	*800*	*850*	*900*	*950*	*1000*
Carbon steel plates	SA-7	...	(1) (3)	60,000	12,650	...	...	...	...	...	...	...
	SA-30	Flange	...	55,000	13,750	13,250	12,050	10,200	8,350	...	...	...
	SA-30	Firebox A	(4)	55,000	13,750	13,250	12,050	10,200	8,350	6,500	...	...
	SA-30	Firebox B	(4)	48,000	12,000	11,650	10,700	9,300	7,900	6,500	...	...
	SA-113	C	(1) (3)	48,000	11,050	...	...	...	...	...	...	...
	SA-129	A, B, C	...	44,000	11,000	...	...	...	...	...	...	...
	SA-201	A	...	55,000	13,750	13,250	12,050	10,250	8,350	6,500	4,500	2500
	SA-201	B	...	60,000	15,000	14,350	12,950	10,800	8,650	6,500	4,500	2500
	SA-212	A	...	65,000	16,250	15,500	13,850	11,400	8,950	6,500	4,500	2500
	SA-212	B	...	70,000	17,500	16,600	14,750	12,000	9,250	6,500	4,500	2500
	SA-283	A	(1) (3)	45,000	10,350	...	...	...	...	...	...	...
	SA-283	B	(1) (3)	50,000	11,500	...	...	...	...	...	...	...
	SA-283	C	(1) (3)	55,000	12,650	...	...	...	...	...	...	...
	SA-283	D	(1) (3)	60,000	12,650	...	...	...	...	...	...	...
	SA-285	A	(2) (4)	45,000	11,250	11,000	10,250	9,000	7,750	6,500	...	...
	SA-285	B	(2) (4)	50,000	12,500	12,100	11,150	9,600	8,050	6,500	...	...
	SA-285	C	(2) (4)	55,000	13,750	13,250	12,050	10,200	8,350	6,500	...	...
	SA-299	...	(5)	75,000	18,750	17,700	15,650	12,600	9,550	6,500	4,500	2500
	SA-300	...	(6)	...	...	...	...	...	...	...	...	...
Chromium-manganese-silicon steel plates												
½ Cr – ½ Mn – ½ Si	SA-202	A	...	75,000	18,750	17,700	15,650	12,600	9,550	6,500	4,500	2500
½ Cr – ½ Mn – ½ Si	SA-202	B	...	85,000	21,250	19,800	17,700	12,800	9,900	6,500	4,500	2500
Nickel steel plates												
2 Ni – 3½ Ni	SA-203	A, D	...	65,000	16,250	15,500	13,850	11,400	8,950	6,500	4,500	2500
2 Ni – 3½ Ni	SA-203	B, E	...	70,000	17,500	16,600	14,750	12,000	9,250	6,500	4,500	2500
2 Ni – 3½ Ni	SA-203	C	...	75,000	18,750	17,700	15,650	12,600	9,550	6,500	4,500	2500
Carbon-molybdenum steel plates												
½ Mo	SA-204	A	...	65,000	16,250	16,250	16,250	15,650	14,400	12,500	10,000	6250
½ Mo	SA-204	B	...	70,000	17,500	17,500	17,500	16,900	15,000	12,750	10,000	6250
½ Mo	SA-204	C	...	75,000	18,750	18,750	18,750	18,000	15,900	13,000	10,000	6250
Manganese-vanadium steel plates												
½ Mn – V	SA-225	A	(7)	70,000	17,500	17,500	17,500	...	...	...	...	...
½ Mn – V	SA-225	B	(7)	75,000	18,750	18,750	18,750	...	...	...	...	...
Chromium-molybdenum steel plates												
½ Cr – ½ Mo	SA-301	...	...	65,000	16,250	16,250	16,250	15,650	14,400	12,500	10,000	6250
Manganese-molybdenum steel plates												
1 Mn – ½ Mo	SA-302	A	...	75,000	18,750	18,750	18,750	18,000	15,900	13,000	10,000	6250
1 Mn – ½ Mo	SA-302	B	...	80,000	20,000	20,000	20,000	19,100	16,800	13,250	10,000	6250

* Boiler Construction Code Table UG-23 (Boiler Construction Code—1950 Edition and subsequent revisions).
(1) See Paragraph UG-6, Boiler Construction Code—1950 Edition.
(2) Flange quality in this specification not permitted over 850° F.
(3) These stress values are one-fourth the specified minimum tensile strength multiplied by a quality factor of 0.92.
(4) For service temperatures above 850° F. it is recommended that killed steels containing not less than 0.10% residual silicon be used. Killed steels which have been deoxidised with large amounts of aluminum and rimmed steels may have creep and stress-to-rupture properties in the temperature range above 850° F., which are somewhat less than those on which the values in the above table are based.
(5) These stress values apply to normalized and drawn material only.
(6) The stress values to be used for temperatures below −20° F. when steels are made to conform with Specification SA-300 shall be those that are given in the column for −20 to 650° F.
(7) Between temperatures of 750 to 1000° F., inclusive, the stress values for Specification SA-212, Grade B, may be used until high-temperature test data become available.

Table 2—Maximum Allowable Stress Values in Tension for Carbon and Low-Alloy Steel Pipes and Tubes in Pounds per Square Inch*

Material	*Pipe or tube*	*Specification No.*	*Grade*	*Notes*	*Spec. min. tensile strength*	*For metal temperatures not exceeding (° F.):*											
						−20 to 650	*700*	*750*	*800*	*850*	*900*	*950*	*1000*	*1050*	*1100*	*1150*	*1200*
Carbon steels	P	SA-53	A	(1) (2)	48,000	12,000	11,650	10,700	9,300	7,900	6,500	...	...	...	...	...	...
	P	SA-53	B	(1) (2)	60,000	15,000	14,350	12,950	10,800	8,650	6,500	...	...	...	...	...	...
	T	SA-83	A	(1) (2)	...	11,750	11,450	10,550	9,200	7,850	6,500	...	...	...	...	...	...
	P	SA-106	A	...	48,000	12,000	11,650	10,700	9,300	7,900	6,500	4,500	2500	...	...	...	...
	P	SA-106	B	...	60,000	15,000	14,350	12,950	10,800	8,650	6,500	4,500	2500	...	...	...	...
	T	SA-192	...	...	...	11,750	11,450	10,550	9,200	7,850	6,500	4,500	2500	...	...	...	...
	T	SA-210	...	...	60,000	15,000	14,350	12,950	10,800	8,650	6,500	4,500	2500	...	...	...	...
Molybdenum steels																	
½ Mo	P	SA-206	P1	...	55,000	13,750	13,750	13,750	13,450	13,150	12,500	10,000	6250	...	...	...	...
½ Mo	T	SA-209	T1														
½ Mo	T	SA-209	T1a	...	60,000	15,000	15,000	15,000	14,400	13,750	12,500	10,000	6250	...	...	...	...
½ Mo	T	SA-209	T1b	...	53,000	13,250	13,250	13,250	13,000	12,750	12,500	10,000	6250	...	...	...	...
Silicon-molybdenum steel																	
1½/ Si – ½ Mo	P	SA-158	P15	...	60,000	15,000	15,000	15,000	14,400	13,750	12,500	10,000	6250	...	...	...	...
Chromium-molybdenum steels																	
½ Cr – ½ Mo	P	SA-280	...	...	55,000	13,750	13,750	13,750	13,450	13,150	12,500	10,000	6250	...	...	...	...
1 Cr – ½ Mo	P	SA-315	...														
	T	SA-213	T12	...	60,000	15,000	15,000	15,000	14,750	14,200	13,100	11,000	7500	5000	2800	1550	1000
1¼ Cr – ½ Mo – ¾ Si	P	SA-158	P11														
	T	SA-213	T11	...	60,000	15,000	15,000	15,000	15,000	14,400	13,100	11,000	7800	5500	4000	2500	1200
2¼ Cr – ¾ Mo – ¾ Si	P	SA-158	P3a														
	T	SA-213	T3	...	60,000	15,000	15,000	15,000	15,000	14,400	13,100	11,000	7800	5500	4000	2500	1200
2 Cr – ½ Mo	P	SA-158	P3b														
	T	SA-213	T14	...	60,000	15,000	15,000	15,000	14,700	14,000	12,500	10,000	6200	4200	2750	1750	1200
2¼ Cr – 1 Mo	T	SA-213	T22	...	60,000	15,000	15,000	15,000	15,000	14,400	13,100	11,000	7800	5800	4200	3000	2000
3 Cr – 1 Mo	T	SA-213	T21	...	60,000	15,000	14,800	14,500	13,900	13,200	12,000	9,000	7000	5500	4000	2700	1500
5 Cr – ½ Mo																	
5 Cr – 1 W	P	SA-158	P5a														
5 Cr – ½ Mo	T	SA-213	T5	(3)	60,000	...	13,400	13,100	12,800	12,400	11,500	10,000	7300	5200	3300	2200	1500
5 Cr – ½ Mo – 1½ Si	P	SA-158	P5b														
	T	SA-213	T13	(3)	60,000	...	13,400	13,100	12,800	12,400	10,900	9,000	5500	3500	2500	1800	1200
5 Cr – ½ Mo – Ti	P	SA-158	P5c														
5 Cr – ½ Mo – Cb	T	SA-213	T16	(3)	60,000	...	13,400	13,100	12,800	12,400	11,500	10,000	7300	4800	2800	1800	1200
5 Cr – ½ Mo – Ti	P	SA-158	P16														
7 Cr – ½ Mo	T	SA-213	T7	(3)	60,000	...	13,400	13,100	12,500	11,500	9,500	7,000	5000	3500	2500	1800	1200
9 Cr – 1 Mo	P	SA-158	P17														
	T	SA-213	T9	(3)	60,000	...	13,400	13,100	12,800	12,500	12,000	10,800	8500	5500	3300	2200	1500
Chromium-vanadium steels																	
1 Cr – V	T	SA-217	T17	(4)	60,000	15,000	...	...	...	...	...	...	...	...	...	...	...

* Boiler Construction Code Table UG-23 (Boiler Construction Code—1950 Edition and subsequent revisions).

(1) For service temperatures above 850° F. it is recommended that killed steels containing not less than 0.10% residual silicon be used. Killed steels which have been deoxidized with large amounts of aluminum and rimmed steels may have creep and stress-to-rupture properties in the temperature range above 850° F., which are somewhat less than those on which the values in the above table are based.

(2) Only (silicon) killed steel shall be used above 900° F.

(3) Maximum allowable stress values for temperatures below 700° F. are: −20 to 400°F, 15,000 Psi; 500°F, 14,500 Psi; 600°F, 14,000 Psi; 650°F, 13,700 Psi.

(4) Between temperatures of 650 and 1000° F., inclusive, the stress values for Specification SA-201, Grade B, may be used until high-temperature test data become available.

Table 3—Maximum Allowable Stress Values for High-

Alloy material	A.I.S.I. type	Specification No.	Grade	Notes	Spec. Min. tensile strength	For metal temperatures not exceeding (° F.): −20 to 100	200	300	400	500	600	650
12 Cr	410	SA-240	A	...	65,000	16,250	15,600	15,100	14,600	14,150	13,850	13,700
12 Cr – Al	405	SA-240	O	...	60,000	15,000	15,000	14,700	14,400	13,950	13,400	13,000
14–16 Cr	...	SA-240	B	...	70,000	17,500	17,500	16,300	15,650	15,100	14,600	14,300
17 Cr	430	SA-240	D	...	70,000	17,500	17,500	16,300	15,650	15,100	14,600	14,300
18–8 (Cr-Ni)	304	SA-167 3 / SA-240 S		(1)	75,000	18,750	17,000	16,000	15,450	15,100	14,900	14,850
18–8 (Cr-Ni)	304	SA-167 3 / SA-240 S		...	75,000	18,750	16,650	15,000	13,650	12,500	11,600	11,200
18–8 Mo (Cr-Ni-Mo)	316	SA-167 11 / SA-240 M		...	75,000	18,750	18,750	17,900	17,500	17,200	17,100	17,050
18–8 Ti (Cr-Ni-Ti)	321	SA-167 5 / SA-240 T		...	75,000	18,750	18,750	17,000	15,800	15,200	14,900	14,850
18–8 Cb (Cr-Ni-Cb)	347	SA-167 6 / SA-240 C		...	75,000	18,750	18,750	17,000	15,800	15,200	14,900	14,850
25–12 (Cr-Ni)	309	SA-167	8	...	75,000	18,750	18,750	17,300	16,700	16,600	16,500	16,450
25–20 (Cr-Ni)	310	SA-167	10	(2)	75,000	18,750	18,750	18,500	18,200	17,700	17,200	16,900
25–20 (Cr-Ni)	310	SA-167	10	(3)	75,000	18,750	18,750	18,500	18,200	17,700	17,200	16,900
	304 L†	...		(1)	70,000	17,500	17,000	16,000	15,000	14,000	13,000	12,500
	304 L†	...		...	70,000	17,500	17,000	13,400	11,000	9,700	9,000	8,750
	316 L†	...		(1)	70,000	17,500	17,500	15,800	14,750	14,000	13,600	13,450
	316 L†	...		...	70,000	17,500	17,500	14,500	12,000	11,000	10,150	9,800
	317 L†	...		...	70,000	Same as Type 316 L						

* Boiler Construction Code Table UHA-23, prepared by Subcommittee on Stress Allowances for Ferrous Materials of the Subcommittee on Materials of the A.S.M.E. Boiler Code Committee.

† Tentative, not yet approved by Boiler Code Committee.

(1) These stresses are permitted when distortion can be allowed without endangering the safety of the structure.

(2) These stress values at temperatures of 1050° F. and above should be used only when assurance is provided that the steel has a predominant grain size not finer than A.S.T.M. No. 6.

(3) These stress values shall be considered basic values to be used when no effort is made to control or check the grain size of the steel.

creep-strength requirement, stress values were also limited to 100%* of the stress to produce rupture at the end of 100,000 hr., the values so chosen being based on a conservative average as evaluated by the Subcommittee. However, in most cases the creep strength is far below the rupture strength. Also, in a few cases the Subcommittee has provided stress values without any rupture test data on the specific composition, such approval being based on tests of materials of similar composition.

In the transition range of temperatures, the stress allowances were limited to values obtained from a smooth curve joining the values for the low and high temperature ranges, the curve lying on or below the curve of 62½% of the minimum expected yield strength on temperature.

In the choice of stress values in the range where a per cent of the tensile strength or yield strength governs, the limitations indicated above have been waived in certain cases, identified by footnote, as it was felt that higher stress values might be justified when deformation was not in itself objectionable, provided all other requirements were met.

Recently proposed revised allowable stress values for the Unfired Pressure Vessel Code (Section VIII, 1950, of the ASME Boiler Code, Table UG-23) are listed in Tables 1 to 3. These stress values have been approved by the ASME Boiler Code Committee and were published in *Mechanical Engineering* for September 1951. Similar stress values for the Power Boiler Code can be found in *Mechanical Engineering*, August 1951.

In the design of equipment not covered by Codes, the design stress values may be decided upon by the manufacturer and purchaser of the equipment and should be based on the best available data plus a knowledge of the expected life of the equipment as well as the operating conditions and the possible hazard to personnel. Rules generally followed are:

1. Up to 750 or 850° F., 25% of the short-time tensile strength and not exceeding 62½% yield strength.

2. Above 900° F., 100% of the stress to produce a second-stage creep rate of 0.01% in 1000 hr., or 80% of the stress to produce rupture in 100,000 hr., whichever is lower.

* This 100% value pertains to the Unfired Pressure Vessel Code (Table UG-23). In the Power Boiler Code (Tables P-5 and P-7) this stress limitation is 60% of the average or 80% of the minimum stress to produce rupture in 100,000 hr. as reported by test data.

ACKNOWLEDGMENT

Useful and helpful comments and suggestions were received from F. L. Hicken, Black, Sivalls & Bryson, Inc.; M. B. Higgins, The Texas Co.; C. H. Samans, Standard Oil Co. of Indiana; Walter Samans, Consulting Engineer; and R. D. Stout, Lehigh University.

Selected Bibliography

Detailed information on creep and stress-to-rupture properties can be found in the articles listed in the bibliography. General discussion of creep properties can be found in references 1, 2, 7 and 12; of creep and stress-to-rupture properties in references 11 and 13;

Alloy Plate Materials in Pounds per Square Inch*

For metal temperatures not exceeding (° F.):																
700	*750*	*800*	*850*	*900*	*950*	*1000*	*1050*	*1100*	*1150*	*1200*	*1250*	*1300*	*1350*	*1400*	*1450*	*1500*
13,400	13,100	12,750	12,100	11,000	8,800	6,400	4,400	2,900	1750	1000	...	...	...	...	...	...
12,450	11,800	11,000	10,100	9,100	8,000	4,000	...	...	...	...	...	...	...	...	...	...
13,900	13,500	13,100	12,500	11,700	9,200	6,500	4,500	3,200	2400	1750	...	...	...	...	...	...
13,900	13,500	13,100	12,500	11,700	9,200	6,500	4,500	3,200	2400	1750	...	...	...	...	...	...
14,800	14,700	14,550	14,300	14,000	13,400	12,500	10,000	7,500	5750	4500	3250	2450	1800	1400	1000	750
10,800	10,400	10,000	9,700	9,400	9,100	8,800	8,500	7,500	5750	4500	3250	2450	1800	1400	1000	750
17,000	16,900	16,750	16,500	16,000	15,100	14,000	12,200	10,400	8500	6800	5300	4000	3000	2350	1850	1500
14,800	14,700	14,550	14,300	14,100	13,850	13,500	13,100	12,500	8000	5000	3600	2700	2000	1550	1200	1000
14,800	14,700	14,550	14,300	14,100	13,850	13,500	13,100	12,500	8000	5000	3600	2700	2000	1550	1200	1000
16,400	16,200	15,700	14,900	13,800	12,500	10,500	8,500	6,500	5000	3800	2900	2300	1750	1300	900	750
16,600	16,250	15,700	14,900	13,800	12,500	11,000	9,750	8,500	7250	6000	4750	3500	2350	1600	1100	750
16,800	16,250	15,700	14,900	13,800	12,500	11,000	7,100	5,000	3600	2500	1450	750	450	350	250	200
12,000	11,500	11,000	...	...	...	...	...	...	...	...	...	...	...	...	...	...
8,500	8,300	8,100	...	...	...	...	...	...	...	...	...	...	...	...	...	...
13,250	13,000	12,700	...	...	...	...	...	...	...	...	...	...	...	...	...	...
9,450	9,100	8,800	...	...	...	...	...	...	...	...	...	...	...	...	...	...

*Same as Type 316 L.

creep and stress-to-rupture data are given in 4; effects of atmosphere in 5, 6.

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The Effect of Fabrication Processes on Steels Used in Pressure Vessels

Discussion by W. B. Brooks

The authors and their sponsor, the Pressure Vessel Research Committee, are to be complimented on making a valuable contribution which emphasizes again the hazards inherent in sheared edges becoming part of the stressed structure of a pressure vessel as in the case of the plates forming a manway or other opening protruding beyond their attachment to the shell of the pressure vessel. It will be recalled that such a condition was considered to be the "trigger" which initiated the failure of a spherical welded tank holding hydrogen.*

The authors conclude "that flame cutting, and especially shearing, lower the notch toughness." Their data indicated that although machined edges are much to be preferred, the flame-cut edges are superior to sheared edges.

Concerning their conclusion that the inferior notch toughness of the flame-cut edges as compared to machined edges is due to metallurgical effects, the writer would like to suggest that with steel of the analysis studied, perhaps the greatest effect of flame cutting is that it upsets the metal immediately adjacent to the edge with the results that when the material has returned to room temperature, this zone is in a state of tension. It follows that local flame normalizing of flame-cut or sheared edges, as the authors suggest, would also upset the metal and cause residual tensile stresses, although these might not be as severe as those existing prior to the treatment.

W. B. Brooks, Texas Division, The Dow Chemical Co., Freeport, Tex.

Paper by S. S. Tör, J. M. Rusek and R. D. Stout, was published in THE WELDING JOURNAL, 30 (9), Research Suppl., 446-s to 450-s (1951).

* Brown, A. L., and Smith, J. B., THE WELDING JOURNAL, 24, 235 (1945).

Author's Reply

The authors thank Mr. Brooks for his comments. They are inclined to believe that the metallurgical effects of flame cutting outweigh the presence of residual stresses. This could be checked by flame normalizing a series of specimens to compare with the as-flame cut and the furnace-normalized series However, previous investigations have failed to show a positive effect of residual tensile stresses on notch toughness.

Dimensional Effects in Fracture

Influence of size and ratios of combined stresses on transition temperature and effect of combined stresses on fracture strength

by C. W. MacGregor and N. Grossman

THREE phases of the general problem of the effects of dimensional changes of test specimens on fracture characteristics are reported. These are (1) the influence of size on the transition temperature from ductile to brittle fracture, (2) the effect of various ratios of combined stresses on the brittle transition temperature and (3) the effect of combined stresses on fracture strength.

In the first problem, flat circular disks of 0.95 C steel, simply supported around the circumference and loaded by a concentrated force at the center, were tested at constant deflection rates and at various constant temperatures in the M.I.T. slow-bend testing device described previously. Sizes were changed in the ratios 6, 2, 1. When the sizes were altered, all dimensions including the disks, supports and loading members were changed in the same proportions. Comparisons were made of the transition temperatures for brittle fracture at the same effective strain rates rather than at equal deflection rates in order to maintain mechanical similitude. It was found that the largest disk of 6 inches diameter had a transition temperature only 8° F. higher than the disk of 1 in. diameter. Thus the size effect for this material is of a trivial nature in ratio ranges of 6 to 1 as far as the brittle transition temperature is concerned.

To study the influence of biaxiality of the stresses on the brittle transition temperature, rectangular plates of 0.95 C steel were simply supported along two sides, free along the other two sides, and loaded by a central concentrated force. The thickness of all of these plates was equal to 1/8 in. The ratios of supported lengths to unsupported lengths varied from 0.5 to 2.667 which produced a variation of the biaxiality of stresses from 0.483 to 0.855. Since the circular disks described previously had a biaxiality ratio of 1.0, the present tests studied the effect of this combined stress ratio between 0.483 and 1.0. It was found if the biaxiality ratio varied from 0.483 to 1.0 that the brittle transition temperautre at a given effective strain rate increased on the average 62° F.

The data obtained in these tests also permitted some conclusions to be drawn regarding the effect of combined stresses on fracture strength. It is shown that as the constraint effect increases, i.e., as the ratio of the biaxial stresses increases from 0.483 to 1.0, the brittle fracture strength also increases markedly. A definite increase in fracture strength is also shown to accompany a decrease in size.

C. W. MacGregor is Professor of Applied Mechanics of the Dept. of Mechanical Engineering and **N. Grossman** is Asst. Prof. of Mechanical Engineering of the Mechanical Engineering Dept. of the Massachusetts Institute of Technology, Cambridge, Mass.

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INTRODUCTION

The influence of size and shape of test specimen on mechanical properties has occupied the attention of investigators for many years, especially since the early work of Barba[1] and Kick.[2] For a homogeneous and isotropic elastic material, mechanical similitude in two geometrically similar bodies of the same material tested at the same temperature and subjected to similarly applied external loads in equilibrium exists when the states of stress (or of strain) are proportional to each other at corresponding points.[3] As far as plastic flow is concerned, Barba and Kick have suggested that to maintain in two geometrically similar bodies of the same homogeneous material similar states of plastic strain requires that they be deformed under equal stress at corresponding locations. This assumes the strain rate has no effect. It is in general necessary to add the condition that at any instant the strain rate is the same in both bodies as well as the strains and stresses for complete mechanical similitude. Even the latter assumes the path of loading or prior strain history is the same for both bodies, the temperature conditions are the same, no phase changes or other metallurgical disturbances take place during loading and that the grain size relative to specimen size has no effect. If brittle fracture takes place with no macroscopic plastic flow, statistical considerations may also influence the mechanical properties.

In many previous experiments purporting to show a size effect one or more of the above conditions have frequently not been met, such as in the experiments of Kuntze[4] where true mechanical similitude was not maintained.[3] Another condition all too prevalent in many experiments indicating a size effect is a lack of control of the strength and ductility properties of the specimens of different sizes at the position in the body where fracture is initiated. When specimens of various sizes are machined from a billet or plate great care has to be exercised that there exists no difference in basic strength and ductility levels between specimens of different sizes at the origin of failure, or else the true size effect is clouded over by non-homogeneities of this sort.

A very important technical problem today is the possible vulnerability of a normally ductile steel to brittle failure. It has been discussed previously[5] that there are many mechanical and metallurgical factors which tend to increase the temperature at which a metal may transform from ductile to brittle behavior. A few of these are increasing triaxiality of the stresses, strain rate, prestrain, prior fatigue cycles, strain aging, grain size, carbon content, pearlite size, etc. When this temperature equals the service or testing temperature, brittle failure may ensue with little or no energy absorption. It is the conditioning of the material for such brittle behavior that is the most important pragmatic question rather than the strength condition or law followed when brittle behavior takes place. Another possible influence on the brittle transition temperature is the size effect. Davidenkov, Shevandin and Wittmann[6] have reported increases in the brittle transition temperature of as much as 38° C. for a 0.25% C steel when sizes of unnotched tension specimens were increased from 2 mm. to 10 mm. They made careful impact tension tests but the

specimens of different sizes apparently had some variation in basic strength and ductility levels which showed up more prominently at the lower temperatures. The reductions in area at fracture from room temperature tests showed a slight decrease as the specimen size was reduced. The properties probably varied somewhat over the section of the bar from which the specimens were machined. The scatter band of the transition temperatures was also greater for each specimen size than the increase in transition temperature found. There appeared to be a limiting size, however, above which little size effect was found. One of the objects of the present study was to investigate under well-controlled conditions the possible size effect as regards the brittle transition temperature. If such were present to any appreciable degree great care would have to be observed in applying the results of small scale laboratory tests to full-scale structures or machine parts in service.

If geometric similarity of test specimens is not preserved it has been well known that considerable changes in brittle transition temperatures may result. Early tests[7] have shown the marked influence of the greater constraint produced by widening a notched-beam impact specimen. Since the exact state of stress in the bottom of the notch for such specimens is practically unknown, it has been difficult to express the degree of constraint produced in notched specimens or to dissociate the pure constraint effect of the state of combined stresses from the effect of the stress concentration. By utilizing plate specimens of different proportions without the presence of a notch, the authors some time ago[8] showed quantitatively for an S.A.E. 1020 steel how increased triaxiality increased the transition temperature. Three cases were tested having ratios of lateral to longitudinal stresses of 1, 0.56 and 0. At least 100° F. difference in transition temperature was found between the uniaxial and equal biaxial stress conditions at the same effective strain rate. Moreover, equivalent notched bars were found which would give the same transition temperature at the same effective strain rate as the simple-plate specimens, thus establishing a structure-notched bar equivalence. Tests showed this correlation to be a purely formal one independent of the material, although different materials had, of course, different base transition temperatures. It is data of this type which would be directly useful in design as discussed[8] previously. The present investigation reported herein carries this phase of the problem further and considers the effect of combined stresses on the transition temperature of a 0.95% C steel for additional ratios of lateral to longitudinal stresses between 1 and 0.

In addition the tests will allow certain conclusions to be drawn regarding the effect of combined stresses on fracture strength.

THE VELOCITY-TEMPERATURE RELATION

Experiments have shown that the testing temperature and the deflection velocity or strain rate can be closely correlated. When the entire temperature-velocity range is considered, use may be made of the velocity-modified temperature concept.[9] For experiments involving brittle fracture by little or no plastic flow and conducted well below room t mperature as in the present case the relationship is

$$\log_e \dot{\delta} = C - \frac{Q}{RT}$$

where $\dot{\delta}$ is the deflection velocity, C a material constant, Q the heat of activation of the material, R the molal gas constant and T the absolute temperature of testing.[10] With a given stable material and system of constraint a straight line is obtained if the logarithm of the deflection velocity is plotted vs. the reciprocal of the absolute temperature of testing. This property is utilized in the present investigation.

MATERIAL TESTED AND SPECIMENS

The material used for this study was Ryerson *VD* (0.95% C) tool steel obtained in the form of a $6^1/_2$-in. diameter bar. The chemical analysis is listed in Table 1. Photomicrographs are included in Fig. 1. The metal was tested in the annealed condition. Figure 2 shows the dimensions of the plate specimens used. The size-effect tests were made on circular disks simply supported along the circumference and bent as a plate by a concentrated load at the center. The diameters were 6, 2 and 1 in. with all other dimensions including thickness, radii of knife edges and supports charged in the same ratio. These are shown in Fig. 3. The specimens used to study the effect of combined stresses on the brittle transition temperature are also shown in Fig. 2 and the supports in Fig. 3. The thickness of the latter was kept constant and the ratios of supported to unsupported lengths varied from 0.5 to 2.667. These plates were thus simply supported along two edges, free along the other two and loaded by a concentrated force at the center.

Table 1—Chemical Analysis

Carbon	1.03
Manganese	0.14
Phosphorus	0.008
Sulphur	0.024
Silicon	0.20
Nickel	None
Chromium	0.15
Vanadium	0.19
Molybdenum	0.02
Copper	0.08
Aluminum	0.04

In order to insure that the portions of the test specimens where fracture originates had the same strength and ductility level for all tests, the specimens were machined in such a way that the center of the specimen under the load coincided with the original axis of the $6^1/_2$-in. diameter bar supplied.

APPARATUS AND TESTING TECHNIQUE

The testing equipment is designed to load a specimen in simple bending at a

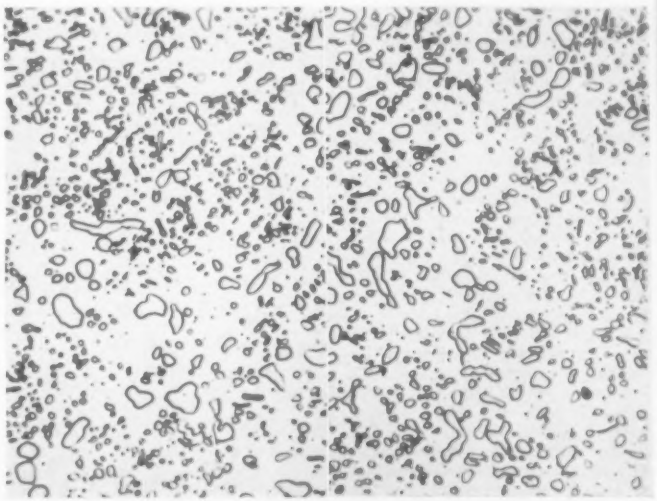
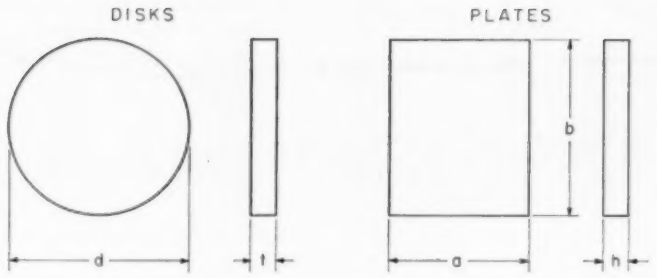


Fig. 1. Photomicrographs of material tested. Magnification: 1000 ×

Longitudinal Section — Transverse Section



d	t
6"	3/16"
2"	1/16"
1"	1/32"

a	b	h
4"	4"	1/8"
1½"	4"	1/8"
1"	½"	1/8"

Fig. 2 Dimensions of specimens tested

given uniform speed and temperature and to supply a load-deflection record of the test. AB-7, SR-4 electric strain gages connected in a bridge circuit are used to measure both the load and deflection. The integral parts of the apparatus include the loading machine with strain gages, the thermocouple and millivoltmeter, stop watch, electronic recorder and a still camera.

The loading device is a lever system which transmits the load to the specimen resting on a suitable support. The support, specimen and electric strain gages are housed in an Aminco "Sub-zero Test Cabinet." The cabinet can be cooled to −100° F. by forced circulation of dry ice, and the specimen can be further cooled by pouring liquid nitrogen around it.

The time of testing is measured either by the axis of the electronic recorder, which places a "dot" on the load-deflection record at regular time intervals, or by a stop watch at the slower speeds.

An a.-c. bridge system is used whereby a 5500-cps. voltage is fed into the load bridge and the deflection bridge with the resulting unbalance from loading or deflecting detected, amplified and sent to a 3FPI tube. A record is made with a still camera. The load bridge and the deflection bridge contain an AB-7, SR-4 strain gage in each of the four arms. The detailed description of the apparatus has been given previously.[8]

The disks and plates were freely supported and centrally loaded with a concentrated load. The specimen was cooled to a temperature slightly above the expected transition temperature and then loaded at a given uniform rate. If a slight yielding took place the test was stopped, the temperature was lowered and the loading process repeated until failure took place with a slight amount of yielding. A new specimen was then tested at a lower temperature to account for the cold working of the trial sample. This procedure was repeated until brittle fracture was experienced with a minimum amount of yielding at the first trial. This same technique was then repeated at some other deflection rate to obtain the strain velocity-brittle temperature relation for the disk under investigation. Thus the results listed do not represent one single test but rather the limit of a number of successive approximations, with the finally satisfactory test value repeated two to three times. The accuracy of the temperature determination is ±3° F. that of the deflection rate ±5%. The amount of macroscopic flow may be judged from a previous study[11] where for a given notched bar a permanent angular bend of 1/2° corresponded to a rise of about 100° F. above the respective transition temperature as determined by the M.I.T. slow-bend test.

Figure 4 includes a view of the apparatus.

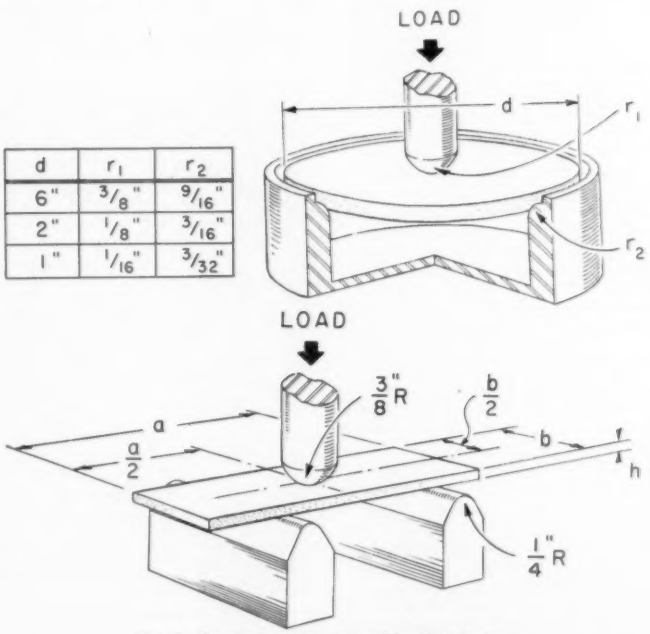
ANALYTICAL CONSIDERATIONS

As discussed previously[6] due to the biaxial nature of the problem it is necessary to utilize composite expressions for stress and strain which depict conditions during the short twilight zone between the cessation of plastic flow and the initiation of brittle fracture. While this is as yet imperfectly understood, it is considered reasonable to utilize those composite values of stress and strain which are valid during plastic flow namely

$$\sigma = \frac{1}{\sqrt{2}}\sqrt{(\sigma_1-\sigma_2)^2+(\sigma_2-\sigma_3)^2+(\sigma_3-\sigma_1)^2}$$

$$\epsilon = \frac{\sqrt{2}}{3}\sqrt{(\epsilon_1-\epsilon_2)^2+(\epsilon_2-\epsilon_3)^2+(\epsilon_3-\epsilon_1)^2} \qquad (1)$$

where σ, σ_1, σ_2, σ_3 are the effective stress, and the three principal stresses respectively and ϵ, ϵ_1, ϵ_2, ϵ_3 are the effective strain and the three principal strains respectively.



d	r_1	r_2
6"	3/8"	9/16"
2"	1/8"	3/16"
1"	1/16"	3/32"

Fig. 3 Specimen supports and loading devices

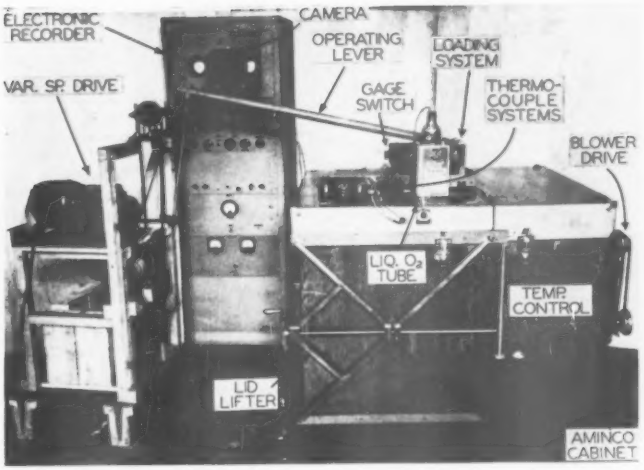


Fig. 4 Load-deflection apparatus

It is then considered that for a stable metal under these conditions that there exists some limited functional relation connecting σ, ϵ, the absolute transition temperature T and the effective strain rate $\dot{\epsilon}$ which is the time derivative of ϵ of the form

$$\sigma = f(\epsilon, \dot{\epsilon}, T) \qquad (2)$$

For the size effect tests with the circular disks, the value of $\dot{\epsilon}$ was determined as follows. The deflection at the center was computed by classical formulas[12] in terms of the central load P. Incidentally, this agreed very well with measured deflections. The maximum stresses were next calculated in terms of P on the bottom side of the disk under the load. The principal strains were then determined from Hooke's generalized law and substituted in (1) to obtain the effective strain in terms of P. Thus the deflection-effective strain relation was determined and consequently the relation between effective strain rate and deflection rate. Hence the deflection rates would be converted to effective strain rates. These showed that to maintain equal effective strain rates in the disks of increasing size it was necessary for comparison to increase the deflection rates proportionately. This is discussed further under Test Results where comparisons are made of the transition temperatures for disks of different sizes.

In the case of the plate tests where the effects of combined stresses on the transition temperature were determined, the procedure was similar. The only difference was that corresponding rectangular plate formulas were utilized.[13]

To represent the effects of constraint, the same index is used as originally proposed by Jackson[14] and utilized formly,[6] namely

$$C = \frac{|\sigma_1+\sigma_2+\sigma_3|}{3\,|\sigma_{\max.}|} \qquad (3)$$

As discussed earlier[6] this factor is 0 for pure shear, 0.333 for pure tension or compression, 0.666 for equal biaxial tensions and 1.00 for triaxial tension or compression.

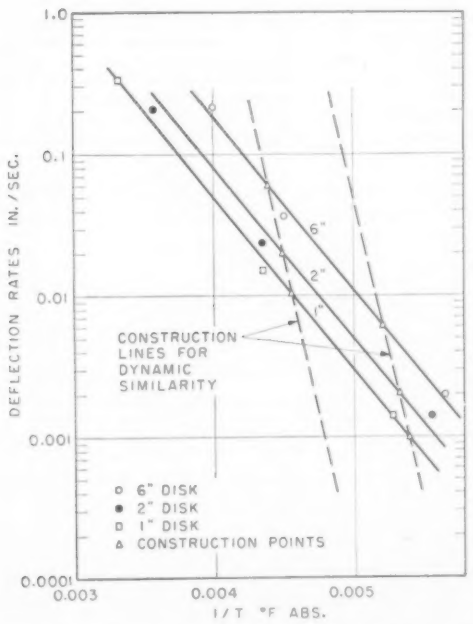


Fig. 5 Size-effect tests. Deflection rates vs. reciprocal of transition temperatures (absolute) for disks tested.

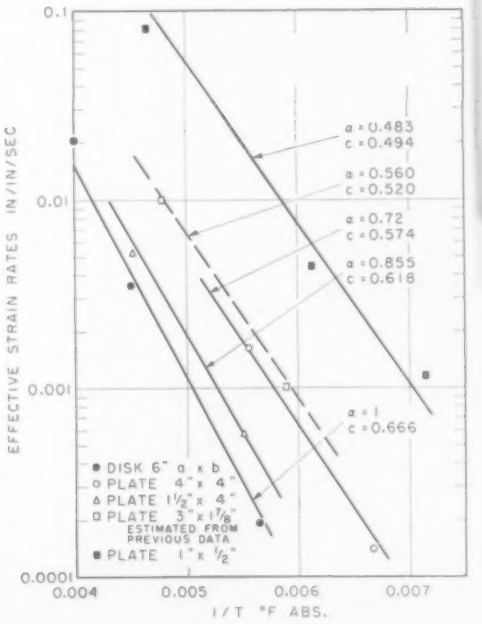


Fig. 6 Effect of combined stresses on transition temperatures

Table 2—Test Results—Size Effect

Disk diam.	Brittle trans. temp., ° F.	T° Abs.	1/T	$\dot{\delta}$, in./sec.	$\dot{\epsilon}$, in./in./sec.
6	−283	177	0.00565	0.0020	0.00019
6	−238	222	0.00450	0.037	0.0035
6	−210	250	0.0040	0.214	0.0203
2	−280	180	0.00556	0.0014	0.0004
2	−230	230	0.00435	0.024	0.00683
2	−180	280	0.00357	0.206	0.0587
1	−270	190	0.00527	0.0014	0.0008
1	−230	230	0.00435	0.015	0.00855
1	−160	300	0.00333	0.57	0.325

Table 3—Test Results—Combined Stress Effects

Plate dimensions	Brittle transition temp., ° F.	T° Abs.	1/T	$\dot{\delta}$, in./sec.	$\dot{\epsilon}$, in./in./sec.
4 × 4 in.	−310	150	0.00667	0.0022	0.000140
4 × 4 in.	−280	180	0.00556	0.026	0.00165
1½ × 4 in.	−279	181	0.00552	0.0012	0.000565
1½ × 4 in.	−239	221	0.00452	0.011	0.00518
1 × ½ in.	−320	140	0.00715	0.00157	0.00118
1 × ½ in.	−265	195	0.00513	0.0060	0.00451
1 × ½ in.	−245	215	0.00465	0.108	0.0811

TEST RESULTS

The effect of size on the transition temperature for the disks (equal biaxial tensions) is shown in Fig. 5 where the logarithm of the deflection rates $\dot{\delta}$ are plotted versus the reciprocal of the absolute testing temperature $1/T$. In order to compare the results at the same effective strain rates $\dot{\epsilon}$ it is necessary to choose deflection rates which increase in direct proportion to the size. Hence auxiliary construction lines are indicated in Fig. 5 which accomplish this. It is thus seen that an almost trivial effect is indicated, namely that the 6-in. disk had a transition temperature of about 8° F. higher than that for the 1-in. disk at the same effective strain rate. Thus while indicating a small increase in transition temperature for this material in the size ranges studied as the size is increased, it is still very small. (Table 2)

Figure 6 shows the effect of combined stresses on the transition temperature. An increase in the ratio of the supported to the unsupported length increased the ratio α of the transverse stress to the axial bending stress from 0.483 to 1.00 while the constraint index C increased from 0.494 to 0.666. It may be seen from this figure as the constraint index C increased from 0.494 to 0.666 that the brittle transition temperature increased on the average 62° F. for this material at a given effective strain rate. The test results for $C = 0.520$ were taken from the previous study[8] for S.A.E. 1020 steel making due allowance for the different base transition temperatures of the two materials. (Table 3)

Figure 7 includes views of the fractures received.

The data obtained also permit a study of the effect of combined stresses on the fracture strength. Table 4 records calculations of fracture stresses, strain rates and strains for the various cases considered. It will be noted that in certain cases checked the fracture deflection calculated agreed quite well with experimental values. Particularly illuminating is the relation between the effective stress and effective strain ϵ (calculated from Equations 1) at fracture. This relation is shown plotted in Fig. 8 for all cases listed in Table 4. It will be noted that all points fall on a straight line, the slope of which is 34.5×10^6 psi. As was shown previously,[15] the theoretical relation between the effective stress σ and the effective strain ϵ for an ideally elastic body is

$$\frac{\sigma}{\epsilon} = \frac{3E}{2(1 + \nu)} \qquad (4)$$

where E is Young's modulus and ν is Poisson's ratio. Using $E = 30 \times 10^6$ psi. and $\nu = 0.3$ for steel Equation (4) shows that

$$\frac{\sigma}{\epsilon} = 34.6 \times 10^6 \text{ psi.} \qquad (5)$$

which agrees well with the slope of Fig. 8. The values for Fig. 8 are for various cases of disks and plates with different ratios of supported to unsupported lengths and were calculated from elastic theory using the experimental fracture loads. Thus Fig. 8 is the elastic $\sigma - \epsilon$ relation for this material. It is worthwhile to note the position on the curve corresponding to various ratios of combined stresses or to values of the constraint index C of Equation 3. It was found possible to utilize the velocity-modified temperature concept[9] and to thus account, for a given constraint factor,

Table 4—Fracture Stress Studies

Test piece designation	Test piece	Brittle trans. temp., ° F.	Exp. $\dot{\delta}$, in./sec.	Expt. $\dot{\epsilon}$, in./in./sec.	Fract. load P, lb.	Calc. fract. stress, psi.	Exp. δ in.	Calc. δ, in.	Calc. ϵ, in./in.	C	α	Effect. stress, lb./in.
A	4 × 4 in.	−310	0.0022	0.000140	1000	σ_x = 169,000 σ_y = 121,700	0.0795	0.0686	0.00436	0.574	0.72	151,000
B	4 × 4 in.	−280	0.026	0.00165	900	σ_x = 152,000 σ_y = 109,000		0.0619	0.00392	0.574	0.72	135,600
C	1½ × 4 in.	−279	0.0012	0.000565	1700	σ_x = 232,000 σ_y = 199,000	0.020	0.0118	0.00630	0.618	0.855	217,000
D	1½ × 4 in.	−239	0.011	0.00518	2060	σ_x = 282,000 σ_y = 241,000		0.0143	0.00765	0.618	0.855	264,000
E	1 × ½ in.	−320	0.00157	0.00118	...	.....	..	..	...	...	...	...
F	1 × ½ in.	−265	0.0060	0.00451	708	σ_x = 171,500 σ_y = 82,800		0.00569	0.00430	0.494	0.483	148,500
G	1 × ½ in.	−245	0.108	0.0811	510	σ_x = 123,500 σ_y = 59,800	0.007	0.00412	0.00310	0.494	0.483	107,000
H	Disk 6 in.	−283	0.0020	0.00019	2980	σ_x = 245,000 σ_y = 245,000	0.072	0.0745	0.00710	0.666	1.00	245,000
I	6 in.	−238	0.037	0.0035	2750	σ_x = 226,000 σ_y = 226,000	0.0675	0.0689	0.00655	0.666	1.00	226,000
J	2 in.	−280	0.0014	0.0004	440	σ_x = 326,000 σ_y = 326,000	0.026	0.0330	0.00940	0.666	1.00	326,000
K	2 in.	−230	0.024	0.00683	410	σ_x = 304,000 σ_y = 304,000	0.026	0.0309	0.00875	0.666	1.00	304,000

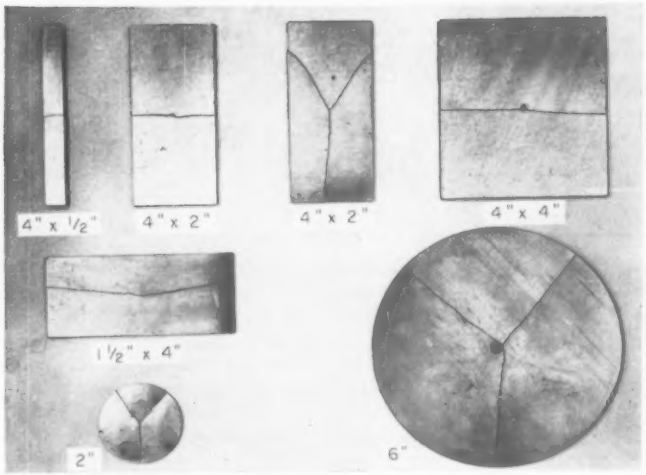


Fig. 7 Typical fracture pictures

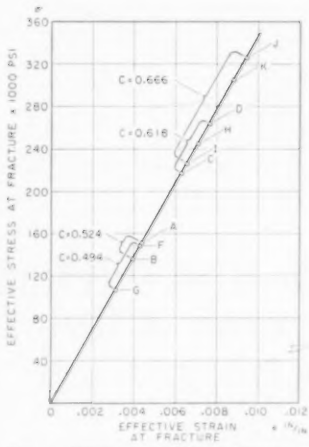


Fig. 8 Effect of combined stresses on effective fracture stresses and strains

for the various strength levels produced by different values of strain rate and temperature. Some overlapping is present, between cases representing various values of C. This is interpreted as reflecting a size effect as indicated by the different positions on the curve for the 2-in. and the 6-in. disks. In spite of this, however, it is unmistakably clear that as the biaxial stress ratio increases, the fracture stress and strain increase. This is further made clear from Table 5 in which the average values of effective fracture strengths are listed for each value of C and α, where by the average is meant the mean point of the range of values of effective fracture strength for each C and α.

Table 5—Increase of Average Effective Fracture Strength with Constraint

C	α	*Av. σ at fracture, psi.*
0.494	0.483	127,700
0.574	0.72	143,300
0.618	0.855	240,000
0.666	1.000	275,200

That the fracture strength increases with triaxiality as shown here, agrees well with the concepts of McAdam, *et al.*,[16] and Sachs[17] and Lubahn[17] and is contrary to the prediction of Fisher[18] and Hollomon.[18] The latter disagree with McAdam, Sachs and Lubahn contending that the increase in fracture strength reported by them as due to increased triaxiality was in reality a size effect and criticized the use of notched specimens to show this. In the present case, however, the use of notches was avoided, thus separating out the complicated effects of constraint and stress concentration, and still the test results show an unmistakable influence of triaxiality in increasing the fracture strength.

More in agreement with Fisher and Hollomon is the size effect on fracture strength shown particularly by the relation between the strengths of the 2-in. and 6-in. disks. Size effect alone cannot, however, explain the increases shown for all the cases reported herein.

CONCLUSION AND SUMMARY

Three phases of the general problem of the effects of dimensional changes of test specimens on fracture characteristics are reported. These are (1) the influence of size on the transition temperature from ductile to brittle fracture, (2) the effect of various ratios of combined stresses on the brittle transition temperature and (3) the effect of combined stresses on fracture strength.

In the first problem, flat circular disks of 0.95 C steel, simply supported around the circumference and loaded by a concentrated force at the center, were tested at constant deflection rates and at various constant temperatures in the M.I.T. slow-bend testing device described previously. Sizes were changed in the ratios 6, 2, 1. When the sizes were altered, all dimensions including the disks, supports and loading members were changed in the same proportions. Comparisons were made of the transition temperatures for brittle fracture at the same effective strain rates rather than at equal deflection rates in order to maintain mechanical similitude. It was found that the largest disk of 6 in. diameter had a transition temperature only 8° F. higher than the disk of 1-in. diameter. Thus the size effect for this material is of a trivial nature in ratio ranges of 6 to 1 as far as the brittle transition temperature in concerned.

To study the influence of biaxiality of the stresses on the brittle transition temperature, rectangular plates of 0.95 C steel were simply supported along two sides, free along the other two sides, and loaded by a central concentrated force. The thickness of all of these plates was equal to $^1/_8$ in. The ratios of supported lengths to unsupported lengths varied from 0.5 to 2.667 which produced a variation of the biaxiality of stresses from 0.483 to 0.855. Since the circular disks described previously had a biaxiality ratio of 1.0, the present tests studied the effect of this combined stress ratio between 0.483 and 1.0. It was found if the biaxiality ratio varied from 0.483 to 1.0 that the brittle transition temperature at a given effective strain rate increased on the average 62° F.

The data obtained in these tables also permitted some conclusions to be drawn regarding the effect of combined stresses on fracture strength. It is shown that as the constraint effect increases, i.e., as the ratio of the biaxial stresses increases from 0.483 to 1.0, the brittle fracture strength also increases markedly. A definite increase in fracture strength is also shown to accompany a decrease in size.

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The Effect of Fabrication Processes on Steels Used in Pressure Vessels

Discussion by Jonathan Jones

In the September *Welding Research Supplement*, Messrs. Tör, Ruzek and Stout report on the effect of different types of edge preparation. It may be said that this article is written from the point of view of the manufacture of pressure vessels to operate at low temperatures, and should not be considered relevant to other types of structure. However, no such limitations are initimated in the Summary, and there is nothing to stop the reader from generalizing from the authors' conclusions. Hence the following comment.

Since the authors' work was with a highly special form of test specimen, the critical spot in which was not an edge in the more general sense, and not prepared as edges are ordinarily prepared, it is worth while to dwell upon this difference.

In the preparation of a plate edge as structural fabricators ordinarily conceive it, where the torch travels on a track, or is guided by a templet, the heating and quenching cannot, or so it would seem, resemble the effect when the torch is used to cut a 1-in. diameter hole. In this latter case it seems as if the heat must be maintained in the vicinity of the previously made kerf, so that the amount of heat which ordinarily would be made in cutting an edge over 3 in. long is practically held in a spot. Neither does this have the annealing effect of our usual postheating torch as used on high-carbon steels; for the cutting flame and annealing flame are totally different in intensity, and in the authors' case it was the cutting flame that was maintained. Furthermore, while punching is related to shearing, the condition of a punched hole is not the same as that of a sheared edge.

The structural engineer relies largely on the failure angle in a slow bend test of a parallel-sided flat plate specimen for his judgment of toughness and suitability. This is a reasonably sensitive test. A piece of 0.40 carbon steel with gas-cut edges will not bend far before it breaks; neither will a piece of 0.20 carbon steel with sheared edges or with edges gas-cut with a manually guided torch. But the 0.20 carbon piece will bend 180 degrees if the edges are machined, or if they are properly gas-cut with a mechanically guided torch; so will the 0.40 carbon piece if its gas-cut edges are properly postsoftened.

These are the tests on which the structural engineer has relied for a generation, in accepting gas-cut edges, mechanically prepared, as the equivalent of machined edges for riveted construction in A7 steel. For welding edges he has supposed that the question did not exist, in view of the fusing temperature to be later applied. Shearing he has accepted for less important edges in his thinner material, only, having regard to the severity and the danger of mechanical tears.

These practices, like any other, he would amend on the basis of new evidence. But not, in all probability, unless the test specimen, the manner of its preparation and the criterion for interpreting the results should seem more closely analogous to his problems than the tests and the criteria he has used hitherto.

Author's Reply

Mr. Jones's comments are valuable and appreciated by the authors.

It is correct to say that this study was made from the point of view of low-temperature service. Any test involving notch toughness implies that the properties of the material at low temperatures and/or under multiaxial straining is of interest. A test of this kind can be only for comparison either between steels or between fabrication methods or between heat treatments. There is no attempt, nor should there be, to conclude that gas-cutting is dangerous to the plate properties. As a matter of fact, the transition temperatures of the gas-cut specimens were very low and would suggest that no serious embrittlement has resulted.

The embrittlement produced by shearing is in good agreement with the experience of many shops, where cracking and failure of sheared plates has often been encountered particularly in cold weather.

These tests were intended to provide a direct comparison between the various methods of edge-preparation, and were mostly useful for the information they furnished about the effects of postheating.

Paper by S. S. Tör, J. M. Ruzek and R. D. Stout published in The Welding Journal, **30** (9), Research Suppl. 446-s to 450-s (1951).

Welding of High-Alloy Castings

Cracks are frequently encountered in welding 15% Cr – 35% Ni castings. A test plate has been developed for determining weldability and qualifying procedures

by R. David Thomas, Jr.

Abstract

Cracks in 1-in. thick welds in 15 Cr–35 Ni (Type HT) castings have been encountered in each of 24 test plates welded by four foundries using a variety of techniques and electrodes. The groove design of the cast plate is considered very satisfactory as a means of evaluating the welding variables in a test program, or as a means of qualifying welding procedures and the weldability of materials. Tensile tests on a few transverse-weld specimens gave ultimate strengths of 40,000 to 45,000 psi. at 1400° F and 17,000 to 21,000 psi. at 1800° F. with fractures occurring sometimes in the weld and sometimes in the base metal.

INTRODUCTION

THE properties of the austenitic heat-resisting alloys have received considerable attention during the past years. The major use of the 15 Cr – 35 Ni alloys is in the form of castings, the properties of which were published by Avery and Matthews[1] in 1947. Several years later Rozet, Campbell and Thomas[2] reported the properties of 15 Cr - 35 Ni weld metals. Problems encountered in actual installations involving welded castings were discussed in a paper by H. J. Nichols.[3] Recognizing that cracks are frequently encountered in the welding of these highly alloyed castings, the High Alloys Committee of the Welding Research Council authorized a program of tests. This report covers the initial phase of this program.

At first it was not entirely clear that a welding problem actually did exist. The Alloy Casting Institute held a symposium to which the High Alloys Committee were invited, which aired the extent of cracking in the foundries' welding shops. Although no conclusions resulted from this symposium, one thing was clear: that the industry needed a standard weld test on which to qualify welding procedures and filler metals and to evaluate the weldability of the base metal.

After two years of preliminary investigations using various types of weld grooves, the Committee established the slotted groove plate shown in Fig. 1. During the preliminary investigations, it was shown that weld-metal cracks and microfissures could be found when the weld-metal composition was low in carbon and high in silicon. When weld-metal cracking was overcome, fusion-line cracks or base-metal cracks were found in highly restrained welds unless a high preheat was used.

PROCEDURE

Four interested foundries were asked to prepare welds in the cast grooved plates. One foundry was requested to cast sufficient plates from a single melt to furnish the material for the entire program. Six plates were welded by each foundry using welding procedures of their own choice. The choice of welding electrode was also left to the foundry except for at least one of the plates, which was to be welded with one standard electrode. Tables 1 and 2 show the weld-metal compositions of the electrodes and the welding procedures used. The finished plates were then radiographed and the films submitted, along with the plates, to Sam Tour and Co. for evaluation.

The evaluation comprised cross-sectioning the welds in three places at approximately 1-in. intervals along the length of the weld. All six faces thus exposed were surface ground and macroetched with aqua regia containing 5 gm. cupric chloride per 100 ml. solution. The cracks found in the macrosections were compared with the indications found in the radiographs. The extent and location of cracks are summarized in Table 3 and representative photomacrographs are shown in Fig. 2.

Having found a number of welds relatively free from cracks, at least in the central portion of the welds, the Laboratory was instructed to machine two transverse 0.505-in. tensile specimens from each of ten plates. The test specimens were polished and etched as before and the observations are shown in Table 3.

R. D. Thomas, Jr., is Vice-Pres. and Director of Research and Engineering of the Arcos Corp., Philadelphia, Pa.

Paper presented at the Thirty-Second Annual Meeting, A.W.S., Detroit, Mich., week of Oct. 15, 1951.

This report covers the initial phase of a project sponsored by the High Alloy Steel Committee of the Welding Research Council, and supervised by a subcommittee comprising the author, as chairman, Howard S. Avery and R. J. Wilcox.

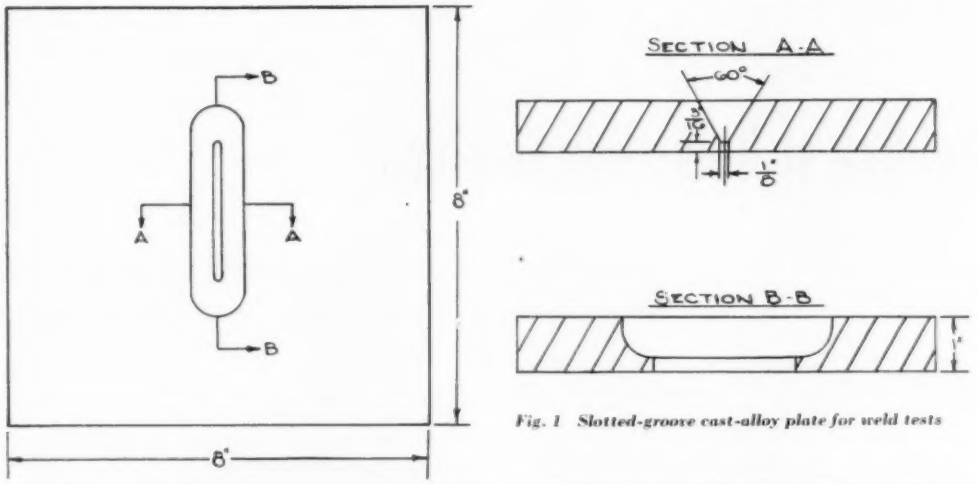

Fig. 1 Slotted-groove cast-alloy plate for weld tests

Table 1—Composition of Casting and Weld Metals*

		C	*Mn*	*Si*	*S*	*P*	*Cr*	*Ni*
Casting		0.49	0.85	1.27	0.035	0.022	17.11	36.88
Electrodes								
5/32 in.	Standard	0.32	2.00	0.28	0.008	0.021	15.61	34.93
1/8 in.	*a*	0.06	1.11	0.35	0.015	0.017	13.74	35.91
6/32 in.	*b*	0.15	1.73	0.49	0.011	0.026	15.77	34.79
5/32 in.	*c*	0.31	0.90	0.24	0.008	0.014	18.19	40.36
3/16 in.	*d*	0.48	1.70	1.06	...	...	17.20	35.57
5/32 in.	*e*†	0.50	...	...	...	...	18.00	35.00
1/8 in.	*f*	0.58	1.84	1.10	...	...	15.94	33.60
6/32 in.	*g*	0.71	1.68	0.92	0.025	0.022	16.84	34.60
5/16 in.	*h*	1.20	1.77	1.43	...	...	15.60	33.80

* Undiluted weld metal from standard weld pads.
† Nominal analysis—complete analysis not available.

Table 2—Welding Procedure Data

Foundry	*Plate no.*	*Welding position*	*Electrode*	*Amperes*	*Preheat*	*Peening*	*Comments*
A	1	Flat	5/32 in. Standard	160	None	None	Split passes used wherever width of groove exceeded 2 1/2 times electrode diameter
	2	Flat	1/8 in. *a*	115	None	None	
	3	Flat	5/32 *b*	160	None	None	
	4	Flat	5/32 in. Standard	120–140	None	None	
	5	Flat	1/8 in. *a*	100–110	None	None	
	6	Flat	6/32 in. *b*	110–140	None	None	
B	1	Vertical	5/32 in. *c*	130	None	None	Vertical plates continuously welded, stopping only to change electrodes.
	2	Flat	1/8 in. *e*	125	None	None	Plate 2: discontinued after 2 layers.
		Flat	6/32 in.	140			
	3	Flat	1/8 in. *c*	120	None	None	Plate 3: full weave for 1st and last 3 layers, others splitpass.
		Flat	5/32 in.	140			
	4	Flat	1/8 in. *b*	120	None	None	Plate 4: 1st pass with 1/8 in. and 2nd with 5/32 in. in flat position, balance in vertical position.
		Flat and vertical	5/32 in.	135			
	5	Vertical	1/8 in. *b*	120	None	Yes	Plate 5: 1st pass only with 1/8 in.
		Vertical	5/32 in.	140	None	None	
	6	Vertical	5/32 in. Standard	140	None	None	
C	1	Length dimension of groove inclined 45 degrees	3/16 in. *d*	200	300° F.	None	Flat layers deposited starting at root and proceeding to top surface of plate, using full-weave technique
	2		3/16 in. *d*	200	300° F.	None	
	3		6/32 in. Standard	150	300° F.	None	
	4		5/32 in. Standard	150	300° F.	None	
	5		3/16 in. *d*	200	300° F.	None	
	6		3/16 in. *d*	200	300° F.	None	
D	1	Flat	1/8 in. *f*		None	None	Plates 1, 3 and 5: full-weave layers.
		Flat	5/16 in. *h*	120	None	None	Plates 2 and 4: split pass for top 4 or 5 layers.
	2	Flat	5/32 in. *g*	80–100	None	None	
	3	Flat	5/32 in. Standard	100	None	None	Plate 6: discontinued after excessive root cracking developed
	4	Flat	5/32 in. *g*	110–120	None	None	
	5	Flat	6/32 in. Standard	120	None	None	
	6	Flat	3/16 in. *h*	120	None	None	

The etch was then removed and the specimens were subjected to short-time tensile testing at 1400 and 1800° F. The testing was conducted in accordance with A.S.T.M. Standard E21-43 using a crosshead speed of 1/4 in. per min. The data from these tests are found in Table 4.

DISCUSSION

It was fortunate that the foundries chose to use radically different welding procedures. By so doing, some quite unexpected results were obtained. On the other hand, had all foundries employed the conventional down-hand welding technique, the results might have been easier to analyze, and the conclusions might have been more clearly indicated. As it is, the results are confused by the many variables and only trends rather conclusions can be indicated.

The six plates submitted by Foundry A were all deposited in about the same manner with two controlled variables. Plates 1 and 4, 2 and 5, and 3 and 6 may be considered pairs, each pair deposited with the same electrode, one at relatively high and the other at relatively low current. During welding, crater cracks appeared more often with low current than with high current, which is inconsistent with the usual experience with austenitic welds. The cracks observed in the

Table 3—Observations of Cracks

Plate no.	*During welding*	*In radiograph*	*In microsections*	*On tensile specimens* —1	—2
A1	3rd layer cracked entire length. Ground out	2 small transverse cracks 1/4 in. long	Several weld-metal cracks in 3 faces 1/32–5/16 in. long	Small pore	One fine crack
A2	None	Crazed crack pattern throughout weld	Numerous fine fissures in all faces		
A3	None	2 longitudinal cracks each 1 in. long	Root-bead crack observed in all faces	3 fine cracks	3 fine cracks
A4	Root-bead crack and crater cracks	One crater crack	Cracks in one face only, one penetrating base metal	None	None
A5	Root-bead crack and crater cracks	Light crazed crack pattern	Numerous fine fissures in all faces		
A6	Crater cracks	Crater cracks and two transverse cracks	Root cracks in two faces only, near crater cracks detected in radiograph	None	1 fine crack
B1	None	Heavy branched crack pattern throughout	Heavy cracking in all faces. Maximum length 1/2 in.		
B2	Cracked badly in second layer. Test discontinued	.....	.....		
B3	None	None. Small area of concentrated porosity	Root cracks in one section. All other faces sound		
B4	Crack found in second layer when plate was in flat position. Test continued in vertical position	Heavy transverse cracks. Some porosity and slag	Heavy cracking on all faces		
B5	None	Similar to B1 and B4 but not so heavy	Same as B4		
B6	None	Similar to B4 and B5	Numerous vertical cracks on all sections. Root cracks on some sections		
C1	None	Slight porosity in center	None except slight porosity in one face	None	2 small pores
C2	None	None	None except one crack 1/32 to 1/16 in. deep found in 2 faces	2 fine cracks	Small gas holes
C3	None	Two crater cracks with questionable longitudinal cracks	All faces showed cracks 1/8 to 1/2 in. deep		
C4	None	Faint questionable indication of longitudinal crack	All faces showed cracks 3/8 to 1/2 in. deep		
C5	None	Faint indication of longitudinal crack	Root crack 1/4 to 3/8 in. deep found in several faces. 1/32 in. base-metal crack. One star-shaped cluster in weld metal		
C6	None	Faint indication of longitudinal crack	Three faces sound. One crack 3/32 to 1/4 in. deep found in two faces, star cluster on remaining face		
D1	None	None	Sound except for root crack 3/32 in. deep found in two faces	None	1 moderate size slag pocket
D2	Cracks in root pass ground out before welding back side	None	None	None	None
D3	None	None	Sound except for seal-bead crack 1/16 in. deep found in 2 faces	None	1/16 in. crack
D4	None	Longitudinal crack full length	Fusion-line crack 1/8 to 1/2 in. deep in all sections. Crack in seal bead in several faces		
D5	None	Slight slag indication at both ends	Sound except for slag pocket in one face	None	1 fine crack
D6	1st pass cracked badly. Test discontinued	.....	.....		

macrosections were somewhat less in the welds deposited at the lower current in two of the three pairs, which is consistent with observations of other austenitic welds. The presence of many fine fissures in the welds made with electrode *a* is not unexpected in view of the very low carbon content. The standard electrode containing 0.32 carbon did not appear to outperform electrode *b* containing only 0.15 carbon, which would not have been expected from earlier work.

Foundry B used both the flat and vertical positions for welding. In all three vertical plates the cracks were numerous. The one relatively sound plate, B3, was welded in the flat position. The electrode used in this plate was not tried in the vertical position and therefore there is some doubt whether the success of this plate is to be attributed to the electrode or to the fact that it was deposited in the flat position. The other two plates which this foundry attempted in the

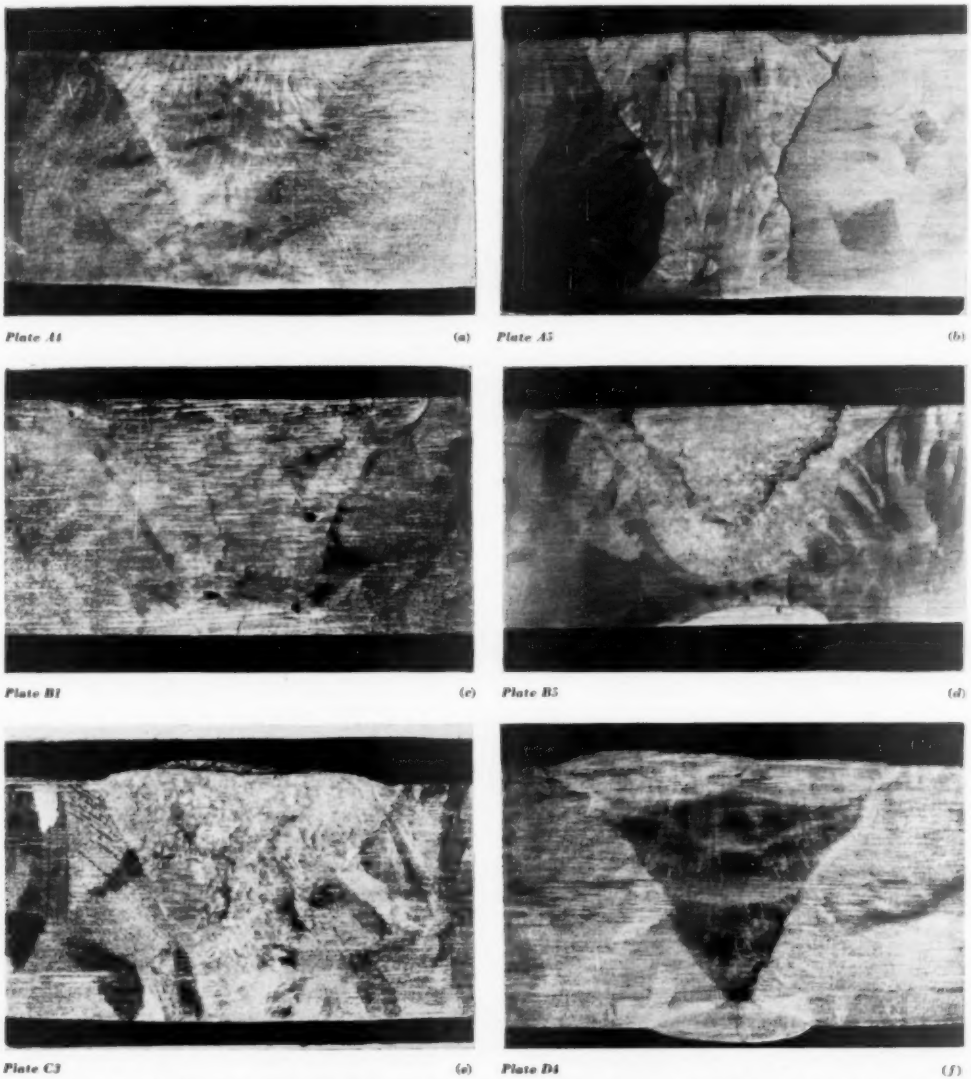

Fig. 2 Typical cracks found on macroexamination: (a) two small fissures one of which extends into base metal, (b) frequent fissures typical in welds with very low carbon content, (c) and (d) cracks in welds deposited using the continuous vertical-welding technique, (e) cracks found in standard electrode weld deposited in the 45° slanting position, (f) fusion-line and seal-bead crack both resulting from incomplete interbead penetration. Magnification 1.5 ×. Etchant: aqua regia with 5 gm. $CuCl_2$/100 ml. solution

flat position resulted in severe weld-bead cracking in the root layers.

The vertical welding technique is rather unorthodox for heavy section butt welds. Instead of layers of weld beads, the arc is struck at the bottom of the groove and played in and out between the root and face, thus depositing the weld metal over the entire thickness of the plates and avoiding the necessity for cleaning the slag, which runs off readily. Welding is continuous except for changing of electrodes, and therefore the rate of heat input is enormous. The shrinkage stresses resulting from the cooling of large masses of metal under large temperature gradients may explain the heavily branched crack patterns found in both the radiographs and the macrosections. Wide weaving of other austenitic welds even in flat-position welding is well known to favor fissure cracking for much the same reason.

Foundry C used an unusual welding technique on all of its plates. By inclining the plates so that the long axis of the groove makes a 45-degree angle with the horizontal plane, the problem of depositing a sound root pass is apparently solved. The groove is filled up in layers, allowing each layer to cool before proceeding. This procedure gave fairly sound welds in four of the six plates, but considerable cracking in the two using the standard electrode. The four plates were welded with an electrode whose composition matched closely that of the casting. It should also be noted that the currents were quite high and that preheat was employed. Consequently the weld metal reaches temperatures at which hot cracking would be expected to occur readily if the composition is susceptible.

Plates C1, C2, C5 and C6 are replicas in all respects except the welder, each being welded by a different man. The two welders responsible for Plates C1 and C2 are apparently superior in their ability to overcome cracking to the two who prepared Plates C5 and C6. This demonstrates the ability of the test plate to evaluate the welder's skill.

The five plates submitted by Foundry D were all quite good. These were deposited in the conventional down-hand position using relatively low currents. No preheat was used but the rate of deposition was such that the interpass temperature increased gradually up to 700 to 900° F. before completion. The only fusion-line crack in the entire program was encountered in Plate D4, illustrated in Fig. 2(*f*). This appears to originate in an area of incomplete fusion at the root and might have been eliminated if the back side had been ground out slightly before depositing the back pass. High-carbon weld metals were employed in all plates except D3 and D5, which were welded with the standard 0.32% carbon electrodes.

Summarizing the welding procedure variables, it appears that continuous welding in the vertical position is not a good practice. Layer welding in a groove inclined 45 degrees looks like a promising procedure. It greatly reduces the degree of restraint and consequently eliminates the weld-bead and crater cracks of down-hand welds, without causing the fissure cracks apparent in the continuous vertical welds. When welding in the flat position great care must be taken to overcome root-bead and crater cracks.

In so far as the electrodes are concerned, it is clearly evident that hot fissure cracks occur readily in very low-carbon weld metals, but at 0.15 carbon and higher the evidence is spotty. Where similar welding techniques are employed, hot tears are usually more frequent with the lower-carbon electrodes. Electrode compositions similar to that of the casting appear to be suitable providing the welding procedures are carefully controlled.

The radiographs cannot be relied upon to detect the fine cracks which are frequently encountered. Vertically oriented cracks as long as $^5/_{16}$ in. fail to show up on the film in some instances. Fine weld-metal cracks which are oriented at an angle of 30 degrees or more to the vertical are not detected.

The short-time tensile tests which are reported in Table 4 are intended to be indicative of the strength of joints. There are too few specimens involved and too many variables to permit much in the way of conclusions to be drawn. The main purpose of conducting these tests at this time was to guide the formulation of further test programs.

In all but one case, the strength of the weld is very nearly equal to the strength of the base metal. When the welds contained no defects as revealed by the pre-testing inspection of the specimens, the failure occurred almost always in the base metal. Conversely, fissures or other defects usually weakened the weld metal sufficiently to cause failure to occur in the weld though

Table 4—High-Temperature Tensile Tests

Specimen no.	*Ultimate strength*	*Elongation in 2 in., %*	*Reduction in area, %*	*Fracture*
		Tested at 1400° F.		
A1-1	40,700	1.5	3.1	Weld and base metal
A3-1	41,000	5.0	7.5	Weld
A4-1	42,000	5.0	9.0	Base metal
A6-1	42,000	5.0	7.0	Base metal
C1-1	43,500	4.0	7.5	Base metal
C2-1	34,400	2.0	3.0	Weld through fissure
D1-1	44,500	2.5	9.0	Base metal
D2-1	45,400	3.5	8.6	Base metal
D3-1	42,300	4.5	7.1	Base metal
D5-1	39,700	5.0	11.6	Weld
		Tested at 1800° F.		
A1-2	18,050	4.0	9.0	Weld
A3-2	18,100	3.5	5.0	Weld
A4-2	19,500	4.0	5.6	Base metal
A6-2	17,300	2.5	8.7	Weld
C1-2	20,600	11.0	11.0	Heat-affected zone
C2-2	17,700	18.0	30.6	Base metal
D1-2	21,200	9.0	29.0	Weld
D2-2	17,700	12.0	26.5	Base metal
D3-2	19,650	5.5	24.1	Base metal
D5-2	17,900	10.5	26.5	Base metal

at strength levels approximating those of the base metal. At 1400° F., one specimen failed prematurely at 34,400 psi. through a fissure observed during the pretesting inspection and the three other specimens, which failed in the weld, had strengths of 40,000 to 41,000 as compared with specimens failing in the base metal at strength of 42,000 to 45,000 psi. At 1800° F., the highest strength recorded, 21,200 psi., represented a weld failure, whereas several specimens failed in the base metal at values below 18,000 psi. The influence of weld fissures is not so clear in the test at 1800° F. as at the lower temperature.

In general, the presence of fine fissures tends to cause failure to occur in the weld but unless the defect is large the reduction in strength is negligible. One should not rely on this observation, however, to condone the existence of fissures, for in future investigations they may be found to jeopardize other high-temperature properties essential to service life.

CONCLUSIONS

A restrained test plate has been devised with which foundries may evaluate the quality of welds from the standpoint of freedom from cracks, fine fissures and other forms of defects. Radiographic examination of the plates will only reveal major defects, but the existence of fine fissures can be determined by cross-sectioning the welds, polishing, and macroetching the weld area.

Fine fissures appear to reduce slightly the tensile strength of the welds at elevated temperatures.

It is hoped that this program may be continued, in order to develop more information on the effect of welding technique on weld soundness, and then having produced sound welds to embark upon a program of tests which would indicate the service properties of the welds.

ACKNOWLEDGMENTS

This work is based upon two reports furnished by Sam Tour and Co., one by Edmond J. Silk and one by Harry Janes. In addition the writer is grateful to the members of the High Alloys Committee of the Welding Research Council for their helpful suggestions, and especially to its chairman, William O. Binder of Union Carbide and Carbon Research Laboratories, and to the members of the writer's subcommittee, Howard S. Avery, of American Brake Shoe Co., and R. J. Wilcox of Michigan Steel Castings Co. For the preliminary test program, the writer gratefully acknowledges the efforts of his able associate, Dr. Hallock C. Campbell.

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Welded Continuous Frames: Plastic Design and the Deformation of Structures

Discussion by
P. S. Symonds

THE purpose of this discussion is to supplement the paper by Yang, Beedle and Johnston first in respect to the function of deflection calculations in the plastic design method, and second with regard to practical methods of calculation of deflection magnitudes for general rigid frame structures.

The plastic design method for redundant structures is based on the concept of failure load as one at which small load increments begin to produce much larger deflection increments than occurred at lower load levels. This type of failure is observed in tests on continuous beams and frames of mild steel as a sharp bend in the load deflection curve, so that with an increase of load of, say, 10% above the knee of the curve the deflection might be doubled or tripled. This failure "load" (actually a narrow range of load) can be predicted by using the concepts of fully plastic moments and of plastic hinges. It is computed as the load at which plastic hinges have formed at a sufficient number of sections to reduce the frame or some part of it to a kinematic mechanism. Under the hypotheses of the theory the frame is incapable of carrying a load above this one, and can deform indefinitely while it is held constant. The deformations are actually limited by effects of strain hardening and shape changes. However a considerable number of tests on beams and frames (see for example reviews by Baker[1] and Maier-Leibnitz[2]) have shown that the simple theory is capable of predicting the observed failure load with reasonable accuracy despite the idealizations involved in the hypotheses.

Using this concept of failure a structure is designed so that the failure load computed for it equals the value obtained by multiplying the working load by the load factor. The latter is a factor chosen by the designer in accordance with his estimate of uncertainties of fabrication and materials, and of possible overloads. The fundamental advantage of the plastic method over elastic methods based on permissible stress values is that the latter cannot deal directly with this real failure of the structure. Hence the designer has no way of knowing what the actual factor of safety is, since this depends on the type and redundancy of the frame and varies in an unknown way (to the elastic designer) from one structure to another.

Moreover it has been shown[1, 3-5] that the plastic failure loads can be computed for complex frames with a remarkable simplicity and quickness as compared with that of analogous elastic analyses.

The plastic methods are directly applicable in cases where the above described concept of failure is appropriate. This includes wartime structures[6] among others in which the ability to carry load without undergoing sudden excessive increases in deformation is the prime consideration. Although the definition of failure involves the rapid *growth* of deflection (per unit load increase) it is not concerned directly with the deflections themselves, and in many structural design problems the actual magnitudes of deflections are governing considerations. This concern with loads rather than (directly) with deflection magnitudes has been recognized as a limitation of the plastic methods (see, for example, a discussion by Winter).[7]

It is clear that if the overriding consideration is a specified deflection magnitude then the fundamental justification of the plastic method no longer holds, since elastic methods can predict values of loads producing failure in this sense as well as a plastic one. In general a frame cannot be designed so as to satisfy simultaneously two independent failure criteria. For example, if the fixed-ended beam considered by Yang, *et al.*, (Fig. 1 (*a*)) is designed so that plastic collapse occurs at a specified load *Pc* then the deflection at collapse is fixed by this condition (within fairly narrow limits set by the available beam sections); and conversely, if the design is such that under a given load a specified deflection is attained, then the beam will not, in general, fail by plastic collapse at this load.

It seems probable that in many cases in which deflection magnitudes must be considered, the plastic failure load analysis can still be used as basis of design, thus taking advantage of the simplicity and directness of the plastic design method, provided the plastic load analysis is supplemented by an analysis to check the values of important deflections. The writer believes that often it will suffice to make a simple analysis which yields approximate values of deflections just as the failure load is attained. In order to make "exact" deflection calculations in the plastic range one should use accurate diagrams of true stress vs. true strain, and take account of the lowered rigidity of members due to the presence of zones of material in which plastic flow has occurred. However, the sizes of these zones and their contributions to the deflections are not easy to calculate, since they depend on the force and moment distributions in the members. Actual deflections will also be influenced by effects such as the variation of yield stress and of the strain at which the material begins to work-harden, which are known to vary widely in as-received beams; and also by peculiarities of support conditions which can hardly be predicted. In many cases, therefore, refined deflection calculations will be of doubtful practical value in the plastic methods, although they will be needed in laboratory research investigations. If highly accurate deflection computations are needed, it seems probable that (rational) elastic design methods will be more appropriate in practice.

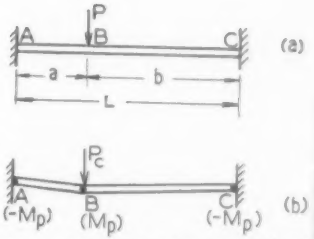

Fig. 1 Fixed-ended beam considered by Yang, Beedle and Johnston. Full circles in (b) show plastic hinges in failure by plastic collapse

The simple method outlined below was first proposed by the writer.[8] It is approximate since it neglects the effects of the

P. S. Symonds is Associate Professor of Engineering, Graduate Division of Applied Mathematics, Brown University.

Paper by C. H. Yang, L. S. Beedle and B. G. Johnston, Progress Report No. 3, was published in THE WELDING JOURNAL, 30 (7), Research Suppl., 348-s–356-s (1951).

plastic zones near the plastic hinge locations. However, it also neglects the strain hardening which almost certainly occurs at most of the plastic hinges. Since these two effects tend to oppose each other, a method neglecting both may be expected to provide reasonable estimates in many cases. To substantiate this, comparisons will be given with deflections calculated by a more refined method, and with a series of test results on small portal frames. Publication of these comparisons was intended earlier, but was unfortunately delayed until the present time.

The method corresponds as far as assumptions are concerned to an approximate method proposed by Yang, Beedle and Johnston. However, the usual failure load of the plastic methods will be taken, namely that at which moments at a sufficient number of cross sections have reached the fully plastic moment magnitude $|M_p|$ to convert the frame or part of it to a mechanism. Yang, *et al.*, recommended using the load at which the moment at the final hinge reaches the yield moment value $|M_y|$, as the load at which deflection values should be computed for design purposes. As another alternative, they suggested replacing M_p everywhere by $|M_y|$. However, the failure load computed according to the usual hypothesis of reaching the value $|M_p|$ at all hinge sections has physical and theoretical significance lacked by the suggested alternatives. It seems preferable to the writer to consider this load and the corresponding deflections as basic, and to make corrections when needed by adjustment of the load factor, or by special methods for certain cases.

A main advantage of the following method over those suggested by Yang, *et al.*, is that it allows deflection estimates to be made for general rigid frames with quickness comparable to that of the failure load calculations. Use of the well-known slope-deflection equations eliminates any dependence on handbook formulas.

To illustrate the method consider first the simple problem of Yang, *et al.*, Fig. 1 (*a*). To compute the collapse load P_c it is necessary only to know that plastic hinges form at sections A, B and C, where the moments are $-M_p$, M_p and $-M_p$, respectively, (Fig. 1 (*b*)). The collapse load is readily found merely from equilibrium (or by using virtual work) to be $P_c = 2LM_p/ab$. The plastic analysis gives no information about the order of formation of the plastic hinges unless a step-by-step analysis is made. This would be very tedious for complex frames, and in the present method is never necessary. In the case with $a < b$ it is obvious that the most highly stressed sections are at A and B so that the last hinge is at C. This means that *just before* the collapse load is attained no rotation of the tangent line at C has occurred, whereas a definite rotation has occurred at A and there is a discontinuity in slope angle at B. The condition of zero rotation at C is sufficient to determine the deflection δ_p under the load by writing the slope-deflection equation* for the segment BC, namely

$$\theta_{CB} = 0 = -\frac{\delta_p}{b} + \frac{bM_p}{6EI}; \quad \delta_p = \frac{b^2M_p}{6EI} \tag{1}$$

* A convenient form of the general equation is

$$\theta_{PQ} = \theta'_{PQ} + \frac{\delta}{l} + \frac{l}{6EI}\,(2M_{PQ} - M_{QP})$$

where θ_{PQ} is rotation at end P of span PQ, of length l and flexural rigidity EI, θ'_{PQ} is rotation at P under conditions of simple support, δ is relative displacement of ends, and M_{PQ}, M_{QP} are end moments. All angles and moments in the above are defined in the positive-clockwise convention, including the angle δ/l.

Note that if the last hinge to form had been (wrongly) assumed to be that at A, then the condition $\theta_{AB} = 0$ furnishes $\delta_p = a^2M_p/6EI$, while if the final hinge had been assumed at B, the condition $\theta_{BA} = \theta_{BC}$ yields the result $\delta_p = abM_p/6EI$. Both of these values are *smaller* (for $a < b$) than the correct result given by Equation 1.

This indicates the procedure which can be applied to general continuous beams and frames. The last hinge to form is not obvious, in general. However, each hinge in the actual collapse mode can be assumed, in turn, to be the last one. Each such assumption corresponds to an assumption of slope continuity or of absence of rotation of a tangent line, and through the slope-deflection equations yields a value of a particular deflection. The *largest* value thus computed is the correct one, as can be easily proved.[5, 6] (Special cases might exist in which the actual deflection is less than this one but these will be rare and of no practical importance.[5]) The work can often be shortened since usually not all hinges need be considered; inspection of deformations will indicate which are the most highly stressed sections in the elastic range, and these can be ruled out as possible locations of the last hinge to form. An equivalent procedure which is useful as a check has also been described.[9] Examples are given elsewhere of applications to a two-span portal frame[5] and to a two-story portal frame.[9]

Applying Equation 1 to the 14 WF 30 beam considered by Yang, *et al.*, with $a = 10$ ft., $b = 20$ ft., $M_y = 1380$ in.-kips., $M_p = 1590$ in.-kips., $W = 41.8$ in.^{3}, $I = 289.6$ in.^{4}, $E = 30{,}000$ kips./in.^{2} and $\sigma_y = 33$ kips./in.^{2}, the values of deflection and load at collapse are found to be $\delta_p = 1.75$ in., and $P_c = 39.8$ kips., respectively. This deflection result may be compared with that obtained by Yang, *et al.*, using their approximate method (Appendix C) which yields results always on the safe side. In this method hinge action is assumed to start as soon as a moment reaches the magnitude M_y, but in computing deflection increments the load increments used are the correct ones with moments at magnitude M_p where hinge rotations have occurred. (This involves an obvious inconsistency.) Using the above data this method yields $\delta_p = 3.04$ in. This method always overestimates the deflection, by what may often be large amounts. A calculation has been made* of the contribution of the plastic zones to the deflection at the collapse load P_c in the present problem. With data as given above the final deflection was found in this case to be $\delta_P = 2.09$ in. This would be reduced if effects of strain hardening were taken into account. The result 1.75 in. yielded by the simple method of this discussion underestimates the actual deflection by an amount probably less than about 15%. The method proposed by Yang, *et al.*, overestimates the deflection by at least 45% in this case. Their method necessitates a step-by-step elastic-plastic analysis, and hence would be lengthy in cases of more complex frames. In view of this, the simpler method proposed by the writer may be considered satisfactory for many purposes, even through the error may not be on the safe side.

To indicate the accuracy of the method in typical cases comparisons with more refined calculations and with test results are of interest. The latter are of more practical importance than the former, since quite elaborate computations are necessary merely to take account of realistic stress-strain diagrams, and it is almost out of the question to include also such effects as inhomogeneity of physical properties, residual stresses, imperfect fixing of supports or continuity of joints, which may well be important in real structures. Hence a theoretical comparison will be given only for a pin-based portal frame (Fig. 2 (*a*)) for which computations were made by Horne,[10] who took account in one case of only the effects of plastic zones and in the other case of effects of both plastic zones and strain hardening, using a standard stress-strain diagram for structural steel. The collapse mode of the frame considered is shown in Fig. 2 (*c*). In Horne's calculations it was assumed that V was increased to the value causing yield to occur in the outer fibers at C, after which H was brought up from zero to the value causing collapse to occur. This order of load application is immaterial for the failure load analysis. It is easily found that at collapse the load and moment values are as follows:

$$H_c = 0.4V_c = 1.14M_p/L,\ V_c = 2.86M_p/L$$
$$M_B = 0.16M_p,\ M_C = M_p,\ M_D = -M_p$$

(Positive moments are those tending to open out the frame.)

In computing deflections the cross section was assumed defined by web area A_w, flange area A_f and depth h as shown in Fig. 2 (*b*), and the special case where

* The writer is indebted to C. F. A. Leth of Brown University for making this calculation.

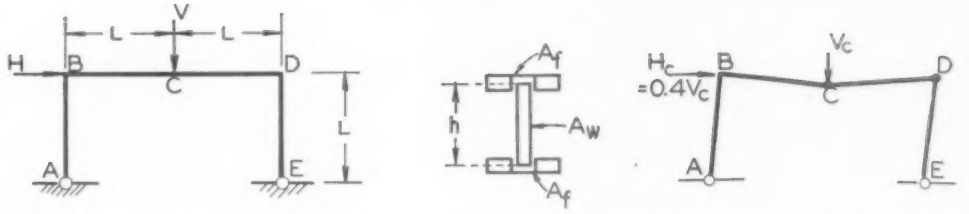

Fig. 2 Pin-based portal considered by Horne[10]

$2A_f = A_w$ was chosen, corresponding roughly to the standard 18 *I* 54.7 section. The sidesway δ_H and vertical deflection at C δ_V are easily calculated approximately by using the condition of slope continuity at C, since it is obvious in this case that the last hinge occurs here. The results are shown in the column labeled "simple method" in Table 1, which gives also the values obtained by Horne by the more refined methods, and the errors.

These comparisons show that for the sidesway (the larger and more important deflection) the two opposing effects in the most accurate calculation nearly cancel each other, and the simple method is here very accurate.

It is of more significance to see how the simple method predicts deflections observed in tests at the theoretical collapse load. A very complete set of test results on small-scale portal frames has been reported by Baker and Heyman.[11] These authors used fixed-base portals with side and vertical loads, as shown in Fig. 3 (*f*). The frames were of mild steel (yield stress 45.5 ± 2.0 kips./sq. in.) with center-line dimensions 2 in. height, 4 in. span, and with legs of square section 1/4 by 1/4 in. Three series of tests were run, in which the horizontal beam had depth 3/16, 1/4 and 5/16 in., respectively, the width in all cases being 1/4 in. Ratios between V and H were chosen in each series so as to produce failure in five different modes. In calculating these load ratios Baker and Heyman took account of the finite cross-section depth, putting hinges at cross sections at the inside corners rather than at intersections of center lines. However, in computing the collapse load and deflection values shown on Figs. 3 (*a*)–(*e*) and in Table 2, using the simple method, only center-line dimensions were used, as would normally be done in practice. The listed load ratios are those used in both tests and present computations.

The load-deflection curves in one series are shown in Fig. 3, with computed deflections. The calculated deflections for all frames tested by Baker and Heyman are given in Table 2 together with the observed values (scaled from the published curves) at the computed failure load. The percentage errors listed show that the errors in the deflections deter-

Table 1—Comparison of Deflections in Example Treated by Horne[10] (Fig. 2)

	Accurate method using standard stress-strain diagram	*Accurate method with strain hardening neglected*		*Simple method*	
	Result	*Result*	*% Error*	*Result*	*% Error*
$\frac{h}{L^2}\,10^3\delta_H$	1.379	1.515	+9.9	1.320	−4.3
$\frac{h}{L^2}\,10^3\delta_V$	0.729	0.848	+15.6	0.621	−10.4

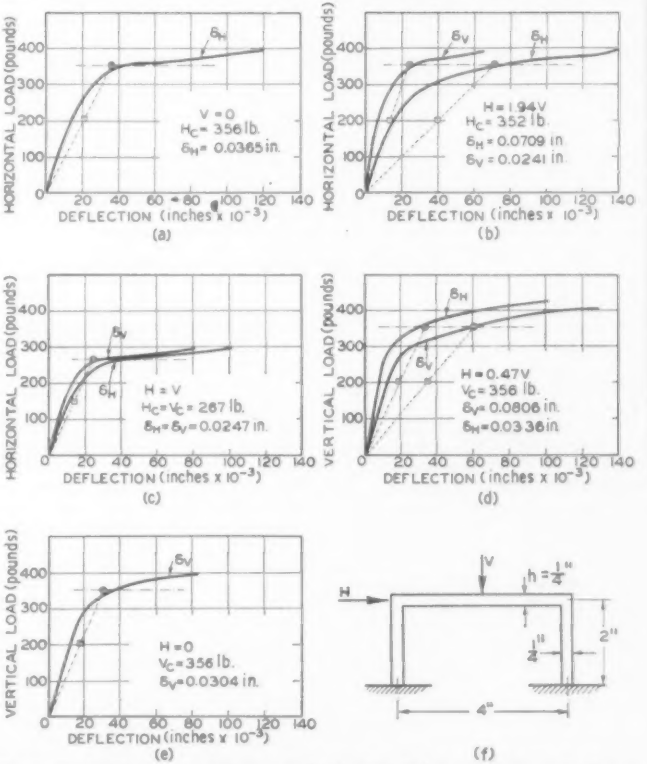

Fig. 3 Typical load-deflection curves obtained by Baker and Heyman.[11] Frame dimensions are shown in (f). Points in circles ⊙ show collapse load and deflection values at imminent collapse as computed by simple method of this discussion. Points in squares ⊡ show failure load and deflection values divided by load factor 1.75. See also Table 2

Comparison with Test Results of Baker and Heyman[11] (All Deflection Values Are in Inches $\times 10^{-5}$)

Series A: beam depth $h = {}^1/_4$ in.

	$V = 0$, $H_c =$ 356 lb.	$H = 1.94V$, $H_c = 352$ lb.		$H = V$, $H_c = 267$ lb.		$H = 0.47V$, $V_c = 356$ lb.		$H = 0$, $V_c =$ 356 lb.
Deflection→	δ_H	δ_V	δ_H	δ_V	δ_H	δ_H	δ_V	δ_V
Observed	44.6	26.9	76.9	29.4	40.0	30.6	60.6	38.3
Calculated	36.5	24.1	70.9	24.7	24.7	33.6	80.6	30.4
% Error	−20	−10	−8	−16	−38	+10	+25	−21

Series B: beam depth $h = {}^3/_{16}$ in.

	$V = 0$, $H_c =$ 278 lb.	$H = 2.53V$, $H_c = 271$ lb.		$H = 1.50V$, $V_c = 151$ lb.		$V = 1.23H$, $V_c = 200$ lb.		$H = 0$, $V_c =$ 200 lb.
Deflection→	δ_H	δ_V	δ_H	δ_V	δ_H	δ_H	δ_V	δ_V
Observed	41.3	29.2	76.9	26.4	35.3	23.8	41.5	24.0
Calculated	33.9	31.0	75.3	24.0	27.1	27.0	46.9	26.5
% Error	−18	+6	−2	−9	−23	+13	+13	+10

Series C: Beam depth $h = {}^5/_{16}$ in.

	$V = 0$, $H_c =$ 356 lb.	$V = 0.72H$, $H_c = 356$ lb.		$V = 1.25H$, $V_c = 352$ lb.		$H = 0.42$ lb. $V_c = 447$ lb.		$H = 0$, $V_c =$ 466 lb.
Deflection→	δ_H	δ_V	δ_H	δ_V	δ_H	δ_H	δ_V	δ_V
Observed	27.8	15.0	37.7	17.2	21.3	21.5	38.8	22.3
Calculated	24.6	17.9	51.5	18.6	22.2	35.2	70.3	36.9
% Error	−12	+19	+37	+8	+4	+64	+80	+65

mined by the simple method are large in some cases, but in most cases (18 out of 24) are less than 25%. Of the six cases in which the error is 25% or greater, in five cases the errors are positive, i.e., the actual deflection is smaller so the error is on the safe side.

When a sufficient number of test results are available, especially on full-scale frames of standard commercial sections, it should be possible to make a statistical study of the errors, and, if necessary, to improve the simple method for the cases in which the errors are consistently large.

A simple but rough method of obtaining deflections under the working load is to divide the deflection computed at the collapse load by the load factor. This has been done using the load factor 1.75 for the frames tested by Baker and Heyman; values are plotted in Fig. 3 but are not listed in Table 2. The computed values are found in *every* case to be larger than the measured ones, and in the cases for which the simple method overestimates the deflection at collapse the error may be very large. Since the error seems to be always on the safe side the method may be satisfactory for cases when only a rough check on the deflection under working loads is needed.

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Dilastrain Method

TWO scientists at Rensselaer Polytechnic Institute, Troy, N. Y., have found a rapid and accurate way for testing steel alloys, other metals and plastics to find out how long they will stand up under normal loads when they are used for making moving parts of machines. A research authority in a national industry has declared it "impossible to exaggerate the importance" of this invention.

The apparatus which Dr. Joseph L. Rosenholtz and Prof. Dudley T. Smith have assembled in their laboratory makes possible the completion in 10 hours or less of a testing job which has been requiring three months or longer on costly machines under expert supervision.

The importance of the Rensselaer discovery to industry of almost every kind, but especially to defense production, rests in the fact that government and industrial laboratories, the world over, are searching constantly for materials and alloys to meet new and demanding standards.

Thousands of tests are run annually to determine if a new material will permit faster and cheaper production, will prevent breakdowns and will save lives in both industry and the armed forces. The Rosenholtz-Smith apparatus permits the rapid and inexpensive testing of materials for tank engines and jet motors, as well as for propellers, turbine blades, revolving shafts and a myriad of other dynamic parts.

The inventors call their discovery the Dilastrain Method. It is based on precise measurements of the extent to which specimens of a given material will expand under controlled temperatures. The patent rights will be held by Rensselaer Polytechnic Institute which will make the Dilastrain Method available under a licensing plan.

Industrial researchers want to know the endurance limit of each new material used for dynamic moving parts. They want to know the load it will bear without giving way as it goes through an infinite number of its movements.

The endurance limit, under the slow and costly testing methods which are employed presently, has been found by subjecting test bars to varying loads or stresses and vibrating them until they either give way or prove sufficiently enduring. This ordinarily takes three months or longer since a half-billion cycles, or complete vibrations, may be set as a practical limit for testing materials which have been designed for moving parts of machines.

The method evolved by the Rensselaer scientists requires the selection of identical specimens of the material and putting them under stress in a definite range. They are then put through an equal number of cycles of vibration so that all will be on an even level of fatigue. This takes a few hours. The specimens are then placed in the apparatus and subjected to controlled temperatures ranging from 20°

(*Continued on page 64-s*)

Stainless-Steel Weld Deposits on Mild and Alloy Steels

Review of published and unpublished information on dissimilar-metal joints made with stainless-steel electrodes; metallurgical characteristics of joints, dilution, mechanical properties, heat treatments, carbon migration, base-steel and weld-metal cracking, hardenable steels and applications

by Helmut Thielsch

SUMMARY

Definition

A DISSIMILAR-metal weld consists of a weld deposit having a chemical composition which differs by at least several per cent from the composition of at least one of the materials on which it is deposited.

The Dissimilar-Metal Joint

In welding operations, part of the base metal is generally melted by the intense heat of the welding operation. The resulting fusion and mixing of the base steel reduces the relative percentage of alloying elements present in the weld metal. This generally is referred to as *dilution*. Some dilution is often desirable since it produces good and uniform bonding between the base steel and the weld metal.

When a ferritic steel is welded with an austenitic stainless-steel electrode, the weldment itself may be considered to consist of three major areas: (1) the heat-affected zone in the base steel, (2) a narrow hardened transition zone in the weld metal along the bond, and (3) the diluted weld metal. In certain welds the hardened transition zone is due to the formation of martensite. In other welds the hardening is believed to be caused by alloying and by stresses which result from the differences in expansion between the austenitic stainless-steel weld metal and the base steel, from carbide precipitation, and from other possible factors. Generally, this hardened zone is not believed to be detrimental—particularly when the carbon content is low.

The amount of dilution and the depth of penetration depend upon different welding conditions. Thus longer heating times and higher temperatures, which are produced by higher welding currents, higher preheating temperatures, etc., increase dilution and alloy diffusion.

Heat Treatment

Although in dissimilar-metal joints the residual stresses may be of considerable magnitude, they are generally not detrimental—unless the steel is exposed to environments which may cause stress-corrosion cracking. Thus, stress-relieving heat treatments to remove residual stresses are rarely necessary. In fact, if the thermal expansion coefficients differ materially, cooling from the stress-relieving temperatures may restore all or at least part of the residual stresses.

Carbon Diffusion

Noticeable carbon migration may occur across the bond whenever any two ferrous alloys of different chemical composition are intimately joined together and are exposed to temperatures at which the diffusion rates of carbon in steel are appreciable. When considerable carbon migration has taken place from the ferritic base steel into the stainless-steel weld metal the strength of the dissimilar-metal joint may be noticeably reduced. Carbon migration is influenced by (1) welding procedure, (2) temperature of heat treatment, (3) metallurgical structure, (4) alloy composition, (5) time at temperature, and (6) stress.

Failures

Failures or cracks in dissimilar-metal joints may occur either in the base metal or in the weld metal. Cracking is usually caused by one or several factors,

Helmut Thielsch is Technical Assistant, Welding Research Council, New York, N. Y.

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such as inherent defects in the material, improper welding technique, thermal or mechanical fatigue, impact shock, stress-corrosion, hydrogen embrittlement, creep, etc. Failure itself is the result of (1) crack formation and (2) crack propagation.

Base-metal cracking in the heat-affected zone may occur during cooling, when the weldment is still at elevated temperatures or after the weldment has cooled down to atmospheric temperatures. In steels of poor quality the stresses produced by the dissimilar-metal joint may cause cracking along the laminations or segregations of non-metallic inclusions in the plate. In hardenable steels base-metal cracks are generally initiated by the formation of small cracks in the martensite. Base-metal cracking may be prevented or minimized by means of suitable preheat and/or postheat treatments. The recommended preheating temperatures depend upon the composition of the base steel and, to a lesser extent, on the welding process and procedure.

Weld metal cracks are the most common and serious defects which may occur in dissimilar-metal joints between a ferritic base steel and austenitic weld metal. Cracks are generally initiated by slag inclusions, porosity, lack of fusion along the bond, or by intergranular weakness in the weld metal which generally leads to hot cracking particularly in restrained root beads.

In specific cases, weld metal failures may be prevented by first lining, "buttering,"* the faces of the weld with 25–20 (Cr-Ni) weld metal. The balance of the weld is then completed with 25–20 or, sometimes, with 18–8 (Cr-Ni) weld metal. However, many investigators prefer welding with 29–9 or 25–12 (Cr-Ni) electrodes to buttering.

Welding

A number of factors should be considered when making dissimilar-metal joints with stainless-steel electrodes. The proper type of electrode should be selected by primary consideration of the expected dilution and, to a lesser extent, the effects of welding procedure, base-metal composition, restraint, and service application. When restraint is severe it is particularly important that electrodes are selected which produce partially ferritic weld metal.

When welding hardenable steels with austenitic stainless-steel electrodes and when preheating is not possible a buttering procedure with small electrodes may often be most satisfactory. When suitable preheat treatments are possible, standard building-up procedures with heavier electrodes may be better.

The effects of corrosion and room- or elevated-temperature applications should also be considered. In a number of corrosive environments dissimilar-metal joints are susceptible to serious local galvanic corrosion and thus may be unsuitable. Certain dissimilar-metal weldments which are completely satisfactory in room-temperature applications may fail when exposed at elevated temperatures. Thus service environment merits serious attention.

* Buttering denotes lining or "cladding" the groove faces of the base steel with a "dissimilar" metal to produce beneficial effects, generally to prevent cracking. The balance of the weld may, and usually is, of the same or of lower alloy composition than the "buttered" layer.

INTRODUCTION

A *dissimilar-metal weld* consists of a weld deposit having a chemical composition which differs by at least several per cent from the composition of at least one of the materials on which it is deposited.

Dissimilar-metal weldments are made when two or more different materials are to be joined by welding. Many examples of such weldments are found in refinery, chemical, and power-plant installations. Low-alloy steel pipes or tubes may be welded to austenitic stainless pumps or turbine casings. Welded joints may be required between certain superalloys and mild, low-alloy or high-alloy steels.

Dissimilar-metal deposits also may be made to join similar metals; for example, during the last war, armor plate, which is made of low-alloy, high-strength steels, was generally welded with austenitic stainless electrodes. Similarly, many low- and medium-alloy and chromium stainless steels are often welded with austenitic stainless steel electrodes. Although in most cases it might often seem desirable to use an electrode having the same composition as the base steel, the very nature of the welding process may produce a weld deposit which has different properties from a corresponding composition in the wrought condition. A number of alloy steels, particularly the hardenable types, may not produce satisfactory weldments. Some alloy-steel weldments exhibit poor mechanical properties or low corrosion resistance in the as-welded condition which is often due to the formation of an undesirable metallurgical structure.

The yield and tensile strength of austenitic weld metal may appear to be low for high-strength welded joints. However, the low yield strength is advantageous in permitting the slight plastic deformation required to distribute load among the imperfectly matched members of all-welded structures. Although it is sometimes not necessary for the weldment to have the full design strength of the base metal when the weld joints are of suitable design and location,[6] it is generally preferred, in good welding practice, to have the weld metal show as nearly as possible the properties of the base steel.

Weld deposits on clad steels, which themselves represent dissimilar-metal joints, also are examples of the use of dissimilar-metal weldments. Here the weld deposit usually has a composition similar to the cladding metal. The dissimilar-metal joint is produced between the weld deposit on the clad side and the weld deposit on the parent metal or backing steel side. The latter weld deposit, as well as the parent metal, usually is a mild steel.

Welded dissimilar-metal joints are generally produced by arc or gas welding, brazing, soldering, or

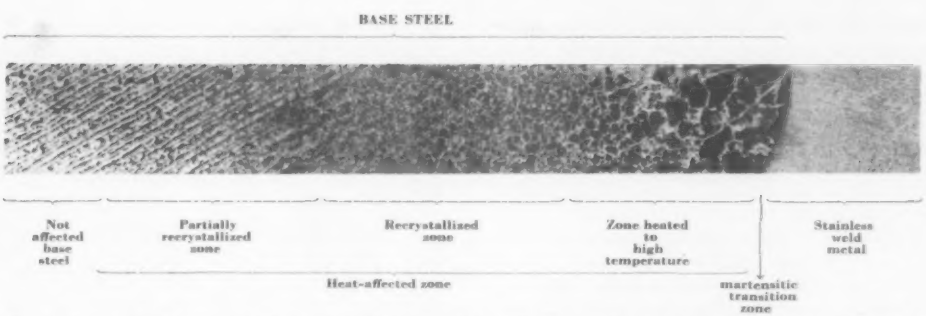


Fig. 1 Structure of base metal, heat-affected zone, and 18–8 (Cr-Ni) stainless weld metal.[52] *Magnification 75 ×*

resistance welding. Moreover, according to Spraragen and Rosenthal,[12] differentiation may be made between two major types of dissimilar-metal joints, that is, between (1) metals dissimilar in the nature of their major constituents (iron and aluminum) and (2) metals dissimilar in the nature of their alloying elements (copper and brass, or mild steels and stainless steels). The latter type of joint is the subject of this discussion which will deal largely with joints between a stainless steel and a mild- or low-alloy steel. Brazed and soldered joints, which employ a non-ferrous material, are not included in this discussion.

THE DISSIMILAR-METAL JOINT

In welding operations, part of the base metal is generally melted by the intense heat of the welding operation. This penetration of the weld metal into the base metal usually is highly desirable since the resulting fusion and mixing between the molten portion of the base metal and the weld metal produces good and uniform bonding between them. When a dissimilar-metal weld is produced, the resulting mixing or fusion is generally referred to as *dilution* because the alloy content of a more highly alloyed material—generally the weld metal—is decreased by being mixed with a metallic composition of lower alloy content—generally the base metal.

Although it is frequently convenient to assume that each weld bead in a dissimilar-metal joint is uniformly diluted, actual determinations have shown that a narrow hardened transition zone is usually present at the bond between the ferritic base steel and the initial austenitic weld deposit. This transition zone is characterized by higher hardness than either the base steel or the stainless steel weld metal. Thus, the dissimilar-metal joint should be considered to consist of three major zones: (1) the heat-affected zone in the base metal, (2) the hardened transition zone, and (3) the diluted weld metal.

The microstructure of a typical transition zone between an 18–8 (Cr-Ni) weld deposit and a mild steel is shown in Fig. 1.[52]

The Hardened Transition Zone

Although in certain welds the relatively high hardness values observed in this zone have been associated with the formation of martensite, in other welds the observed high hardness values are attributed to factors other than the presence of martensite. In these latter cases the increase in hardness has been ascribed to alloying and to stresses caused by the differences in expansion of the austenitic stainless-steel weld metal and the base steel, to carbide precipitation, etc.[64]

The presence of martensite (also called core-martensite) in the hardened transition zone of the weldment shown in Fig. 1 is illustrated in Fig. 2 at a greater magnification. The weld deposit was made from an 18–8 (Cr-Ni) electrode.[52]

The absence or presence and, in the latter case, the width of the martensitic transition zone depends on

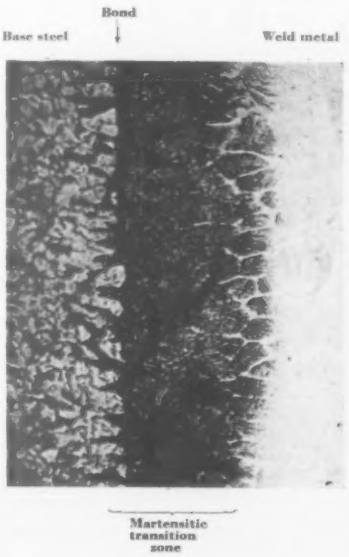


Fig. 2 Structure of martensitic transition zone in the 18–8 (Cr-Ni) weld metal shown in Fig. 1.[52] *Magnification 400 ×*

the initial dilution and on diffusion of alloying elements. It is a function of (1) the composition of the base steel and of the electrode material, (2) the amount of dilution and penetration, (3) the cooling rate, and (4) subsequent heat treatments. In many welds the action of these factors prevents martensite formation and results only in leaving a narrow zone exhibiting high hardness values. For example, increases in the amount of austenite-forming elements in the electrode alloy reduce the width of the martensite zone and, in many compositions, prevent martensite formation. Thus, whereas many investigators seem to have observed a narrow martensite zone in 18–8 (Cr-Ni) weld deposits, as shown in Fig. 2, in 25–20 (Cr-Ni) weld deposits the presence of such a zone is generally not observed. The example in Fig. 3 shows Eberbach microhardness determination across the transition zone of a 25–10 (Cr-Ni) weld deposit on a low-alloy steel (0.30 C, 3 Ni and 1 Cr). In this case the higher hardness values are ascribed to martensite formation due to alloying caused by diffusion as a result of heating from multibead welding and a subsequent stress-relieving heat treatment.[114] Stresses, as mentioned earlier, are also likely to have been a contributing factor to the higher hardness values. Alloy diffusion may have been negligible in single weld deposits which have not experienced subsequent heating caused by multibead welding or heat treatments—particularly when the weldment was rapidly cooled. Under these conditions some investigators believe that, since the molten base steel and the weld metal have mixed completely, the hardening in the transition zone can be fully explained by stresses.[64] This has been observed even in 18–8

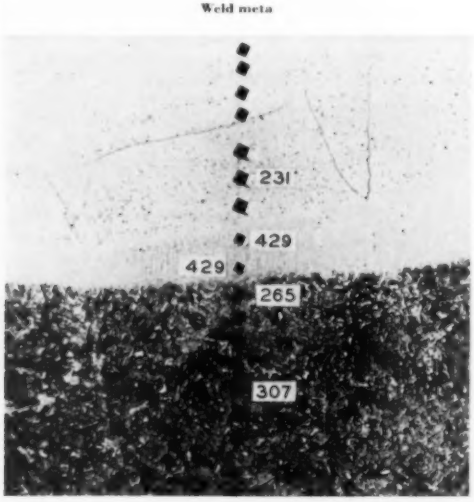


Fig. 3 Eberbach microhardness determinations across hardened transition zone between austenitic 25–10 (Cr-Ni) weld metal deposited on a low-alloy steel.[114] Magnification 200 ×

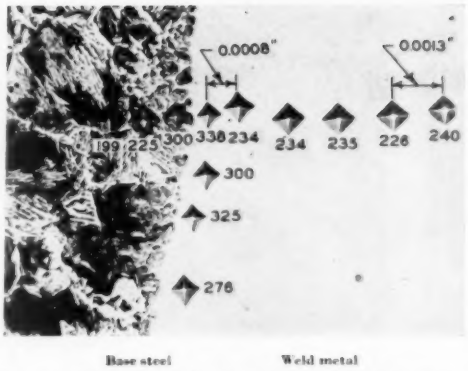


Fig. 4 Vickers microhardness determinations across hardened transition zone between austenitic 18–8 (Cr-Ni) weld metal deposited on a mild base steel[64]

(Cr-Ni) weld deposit on a carbon steel which did not exhibit a martensitic transition zone as shown in Fig. 4. The small increase in hardness on either side of the bond was practically symmetrical and, as such, was believed to have been caused by stresses alone which were the result of the difference in expansion of the austenitic and ferritic materials.[64]

The question of whether the hardness increase is due to martensite formation, or to residual stresses, or to both, is primarily of academic interest. Unless cracking has occurred during the initial cooling, this hardened transition zone is generally believed to be not detrimental. One exception may be the fatigue properties which are likely to be somewhat lowered by the existing residual stresses.

Service applications or heat treatments above 1050° F. (565° C.) generally will soften the martensite, when it is present, and may reduce somewhat the residual stresses. Results of exposure at 1050° F. (565° C.) on the martensite transition zone in an 18–8 (Cr-Ni) weld deposit are shown in Fig. 5.[84] Heat treatments (or exposure) of these and similar joints above 1250° F. (675° C.) generally would not be recommended because at these and higher temperatures carbon migrates at a considerable rate from the ferritic base steel into the austenitic weld metal.

DILUTION

Dilution is a term used to describe the mixing of the deposited electrode metal with that part of the base steel which is molten during the welding operation. The resulting weld metal, therefore, is a mixture of electrode metal and base steel.

Problems of dilution are most important when austenitic weld metal is deposited on mild, low-, and medium-alloy steels and on martensitic or ferritic chromium stainless steels. Dilution of the austenitic weld metal changes the relative amount of ferrite and austenite forming elements. Thus, depending on the

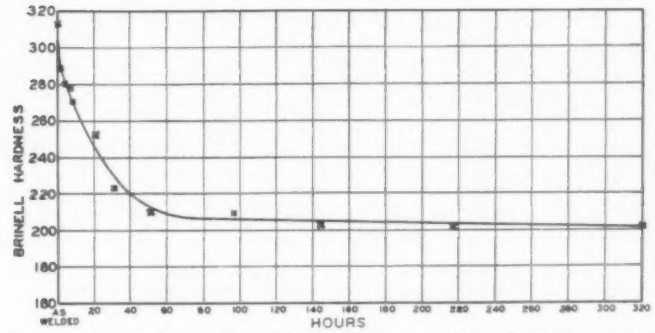


Fig. 5 Brinell hardness values of heat-affected zone in 2¼%Cr-1Mo base steel welded with 18-8 (Cr-Ni) electrode metal after exposure at 1050° F. (565° C.)[11]

amount of dilution and the resulting composition, some or even considerable quantities of ferrite (or martensite) may form in the weld metal. The relation between dilution and transformation characteristics of 19-9 and 25-20 (Cr-Ni) electrode materials are shown in Figs. 6 and 7.[73*] The base steel was a hardenable chromium-nickel alloy steel containing:

C, %	0.32
Mn, %	0.23
Si, %	0.01
Cr, %	1.33
Ni, %	2.95
Mo, %	0.00
P, %	0.013
S, %	0.030

* A paper in which these results are discussed in detail is now being prepared by W. H. Wooding for publication.[73]

To simulate the widest possible range of weld metal dilution the specimens were prepared by melting of suitable quantities of base steel and electrode metal in an induction furnace. The respective electrode materials were of the following composition:[73]

	19-9 (Cr-Ni) electrode material, %	*25-20 (Cr-Ni) electrode material, %*
C	0.07	0.09
Mn	1.75	1.68
Si	0.35	0.30
Cr	21.08	26.50
Ni	11.23	21.30
Mo	0.04	0.05
P	0.010	0.015
S	0.012	0.010

The compositions of the diluted metals are given in Tables 1 and 2.

The transformation characteristics of each diluted alloy were determined by means of a dilatometer.[73] Thus, as shown in Fig. 6, the weld metal from this 21-11 (Cr-Ni) electrode could be expected to be diluted by as much as 35% of the alloy base steel and still produce an essentially austenitic weld-metal structure. The 25-20 (Cr-Ni) electrode metal could be expected to produce fully austenitic weld deposits up to a dilution of about 60% by the base steel, Fig. 7. Of course, mild steels and alloy steels of lower nickel content would reduce the respective dilution limits at which the weld metal would tend to become partially ferritic (or martensitic).

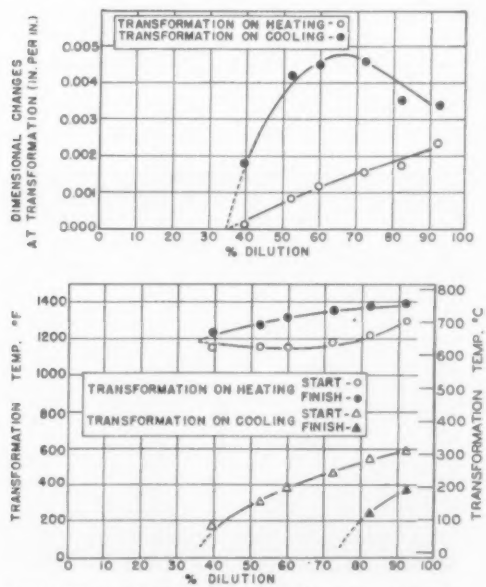

DIMENSIONAL CHANGES AT TRANSFORMATION (IN PER IN.)
TRANSFORMATION ON HEATING - ○
TRANSFORMATION ON COOLING - ●
% DILUTION
TRANSFORMATION TEMP. °F
TRANSFORMATION TEMP. °C
TRANSFORMATION ON HEATING START - ○ FINISH - ●
TRANSFORMATION ON COOLING START - △ FINISH - ▲

Fig. 6 Effect of dilution on the transformation characteristics and temperatures of 19-9 (Cr-Ni) stainless-steel electrode materials[73]

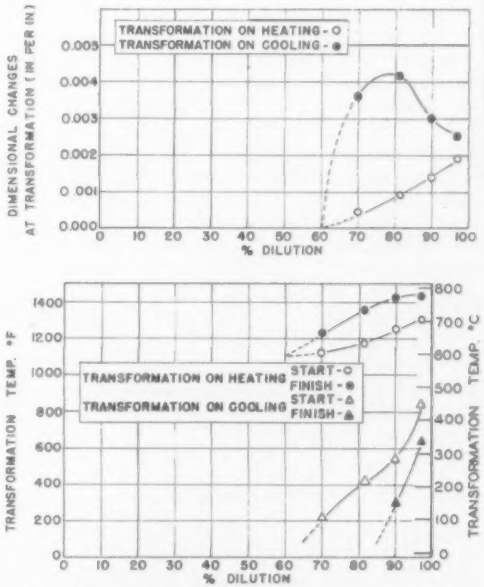

DIMENSIONAL CHANGES AT TRANSFORMATION (IN PER IN.)
TRANSFORMATION ON HEATING - ○
TRANSFORMATION ON COOLING - ●
% DILUTION
TRANSFORMATION TEMP. °F
TRANSFORMATION TEMP. °C
TRANSFORMATION ON HEATING START - ○ FINISH - ●
TRANSFORMATION ON COOLING START - △ FINISH - ▲

Fig. 7 Effect of dilution on the transformation characteristics and temperatures of 25-20 (Cr-Ni) stainless-steel electrode materials[73]

Table 1—Chemical Composition of Heats Representing Various Dilution Levels of 19-9 (Cr-Ni) Electrode Materials[73]

Melt heat No.	Desired dilution, %	Actual dilution, %	Analysis, % C	Mn	Si	Cr	Ni	Mo	P	S
1	10	8.00	0.09	1.06	0.22	18.32	10.38	0.48	0.019	0.022
2	20	15.08	0.12	1.05	0.25	16.56	10.02	0.43	0.017	0.014
3	30	27.50	0.14	0.98	0.19	14.53	9.03	0.34	0.017	0.008
4	40	39.80	0.18	0.74	0.07	12.39	7.77	0.55	0.014	0.025
5	50	52.50	0.21	0.62	0.17	10.42	6.42	0.48	0.021	0.015
6	60	60.00	0.23	0.58	0.09	8.66	6.10	0.43	0.018	0.012
7	70	72.50	0.19	0.44	0.06	6.57	5.36	0.35	0.014	0.012
8	80	82.50	0.24	0.37	0.05	4.72	4.26	0.26	0.015	0.021
9	90	92.50	0.23	0.27	0.04	3.04	3.81	0.18	0.014	0.020

Table 2—Chemical Composition of Heats Representing Various Dilution Levels of 25-20 (Cr-Ni) Electrode Materials.[73]

Melt heat No.	Desired dilution, %	Actual dilution, %	Analysis, % C	Mn	Si	Cr	Ni	Mo	P	S
1	40	40.00	0.18	1.42	0.38	16.30	13.67	0.07	0.021	0.018
2	45	45.00	0.17	1.27	0.26	15.18	12.84	0.02	0.021	0.021
3	50	51.00	0.20	1.09	0.22	13.75	11.84	0.04	0.019	0.024
4	60	60.50	0.20	1.00	0.13	11.25	10.08	0.02	0.019	0.018
5	70	70.50	0.22	0.82	0.11	8.72	8.33	0.03	0.019	0.026
6	80	81.50	0.20	0.70	0.09	6.19	6.12	0.03	0.016	0.024
7	90	90.00	0.19	0.44	0.04	4.03	4.91	0.03	0.017	0.022
8	95	96.50	0.25	0.14	0.05	2.18	4.08	0.02	0.013	0.016

The amount of dilution and the depth of penetration primarily depend upon the welding conditions. Thus longer heating times and/or higher temperatures resulting from higher welding currents or heavier electrodes etc., increase dilution and alloy diffusion.[16]

Although it is unlikely[15, 95] that there is uniformity in the weld-metal composition, it is generally convenient to assume that each respective weld bead is of uniform composition (i.e., has been uniformly diluted). In most cases this assumption may be quite valid. However, as the thicknesses of the base-steel section and of the electrode are increased, the cooling rates, which become more rapid with increases in the thickness of the steel, may cause sufficiently rapid solidification to prevent uniform mixing of the molten weld-metal pool. This is particularly true in the initial root pass which generally mixes with a greater amount of (molten) base steel and solidifies more rapidly than the subsequent passes which are deposited on material heated by the initial deposit. The weld shown in Fig. 8 was made from an 18-8-Mo (Cr-Ni-Mo) electrode with a straight forward movement. According to Granjon[77] the structure consisted of alternating zones of austenite and austenite-martensite, as verified by microscopic and microhardness determinations. It was believed that these two alternating elongated zones are caused by the drop-like transfer of weld metal from the electrode to the weld deposit and by discontinuous melting of the base steel. (See also p. 48-s.)

Estimation of Metallurgical Structure

A simple method, allowing the estimation of "uniform" chemical and structural changes in austenitic weld metal which was deposited on mild and alloy steels, has been developed by Schaeffler[18, 60] and has been used by various investigators in predicting the composition and structure of single and multibead weld deposits in dissimilar-metal joints.[19, 20, 31, 50, 59, 70, 73]

The Schaeffler diagram provides a method for predicting the structure of deposited weld metal based on its composition. Since, in the welding of dissimilar metals, the resulting weld deposit may be assumed to be a mixture of the electrode composition and of the base metal (or metals), the diagram is useful in predicting the microstructure and composition of the "diluted" weld deposit. As is illustrated in Fig. 9, the composition of the base metal and the composition of the undiluted weld metal (or of the electrode—disregarding slight losses of certain alloying elements during metal transfer while welding which are caused by oxidation, volatilization, etc.) are located on the diagram. By drawing lines joining the weld-metal

Fig. 8 Alternating zones of austenite and austenite-martensite in 18-8-Mo (Cr-Ni-Mo) root weld deposit.[77] Magnification 3½ ×

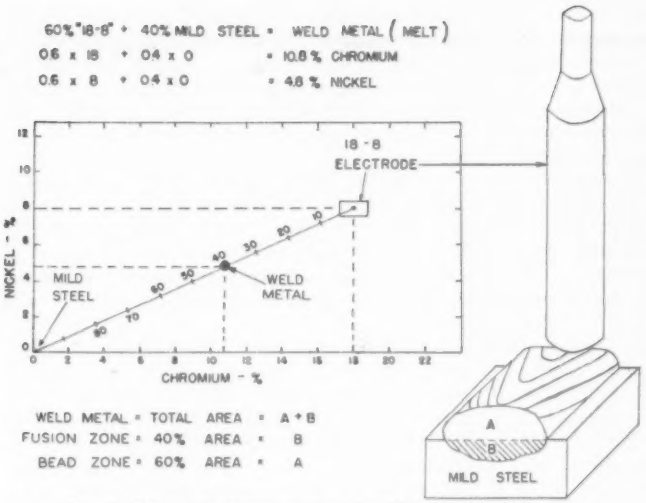

60% "18-8" + 40% MILD STEEL = WELD METAL (MELT)
0.6 x 18 + 0.4 x 0 = 10.8 % CHROMIUM
0.6 x 8 + 0.4 x 0 = 4.8 % NICKEL
18 - 8 ELECTRODE
MILD STEEL
WELD METAL
NICKEL - %
CHROMIUM - %
WELD METAL = TOTAL AREA = A + B
FUSION ZONE = 40% AREA = B
BEAD ZONE = 60% AREA = A
A
B
MILD STEEL

Fig. 9 Schematic determination of weld-metal composition[55]

composition and the base-steel composition, the structure and composition of the resulting weld metal can be predicted on the basis of the dilution or mixing. The lines have been identified by Schaeffler[55, 69] as *dilution direction lines* because they indicate the locus of compositions and structures which are possible with various amounts of dilution or weld-metal penetration.

In the example shown in Fig. 9, deep penetration was assumed so that the weld metal was diluted by 40%, i.e., the deposit consisted of 40% base metal and 60% electrode metal. When the compositions of the electrode and base metal are known only as alloy types, the range of possible compositions is generally indicated by squares or rectangles. The center of the square or rectangle is then assumed as the composition of the undiluted weld metal.

Two actual examples are illustrated in Fig. 10. Here, electrode metal from a 19–9 (Cr-Ni)—Type 308—and a 25–20 (Cr-Ni)—Type 310—electrode is deposited on a low-alloy, chromium-nickel steel containing 0.40% carbon (SAE 4340). If a dilution of about 30% is assumed and the 19–9 (Cr-Ni) electrode is used, the resulting weld metal composition could be expected to exhibit an austenite-plus-martensite structure which tends to be somewhat brittle. Greater dilutions would have resulted in greater quantities of martensite and in correspondingly higher hardness values and a greater tendency for weld-

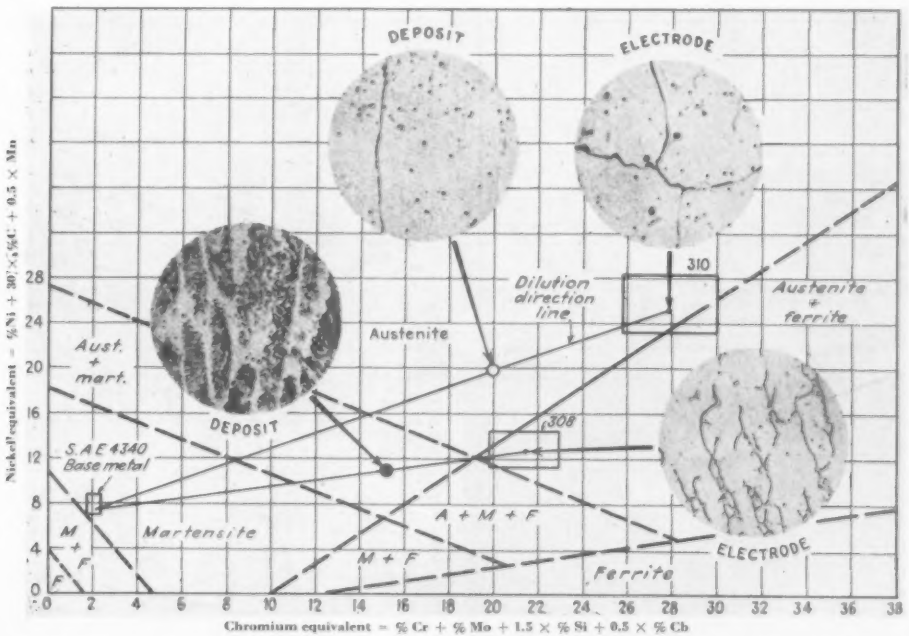


Fig. 10 Dilution direction lines to predict the microstructure of the weld deposit when welding dissimilar metals. Photomicrographs show weld-metal structure resulting from depositing Types 308 and 310 stainless-steel electrodes on a S.A.E. 4340 base steel[55]

metal cracking. On the other hand, with a dilution of the weld metal of only 10%, a partially austenitic weld deposit would have been expected which contains a small amount of ferrite—generally a desirable weld-metal structure. If a 25–20 (Cr-Ni) electrode were used in welding the SAE 4340 steel and if the dilution had again amounted to 30%, the resulting weld metal would be expected to be fully austenitic corresponding to a locus on the dilution direction line at a 20% equivalent nickel—20% equivalent chromium composition.

These results show that, if a fully austenitic weld deposit is desired on an SAE 4340 steel, either a 25–20 (Cr-Ni) electrode should be used or, if the partially ferritic 19–9 (Cr-Ni) type of electrodes is preferred, welding processes or procedures should be used which keep dilution of the 19–9 electrode material to below 10%.

The same reasoning applies to weld deposits on two different base metals and to multipass welding procedures. Typical results of the multipass welding of an SAE 4340 alloy steel to a 16–25–6 (Cr-Ni-Mo) stainless steel with 29–9 (Cr-Ni) electrodes are shown in Fig. 11. The compositions and structures which each weld bead may be expected to contain are indicated in parts *A*, *B*, *C*, and *D*.

The greatest importance of Schaeffler's method lies in the fact that this procedure allows selection of the proper stainless-steel electrode in the welding of steels having compositions* which correspond to those of the structural chromium-nickel equivalent diagram. Where this diagram is applicable, weld metal defects or failures of the welded joints in service might have been avoided if they were caused by incorrect electrode selection.

The amount of dilution can be estimated experimentally by sectioning the weld and by determining the relative area that the weld bead penetrates the base metal. Generally, however, such accurate determinations are not necessary since the rough estimation of the expected dilution would usually be sufficient.

For bead-on-plate welds with the metal-arc welding process at normal currents and arc voltages, the dilution will usually vary between 25 and 40%.[48] Ordinarily, metal-arc welding of root passes in butt welds usually results in dilutions ranging from 30 to 50%.[48] Nevertheless, by keeping down the welding variables which promote dilution (e.g., welding current, welding speed, etc.) the first pass of a butt weld, not backed up, may result in dilutions as low as 10 to 30%. Dilution is generally somewhat lower in the first pass of a backed-up weld such as the first pass in welding stainless clad

* Heat-resisting and other alloy materials would require the development of similar diagrams.

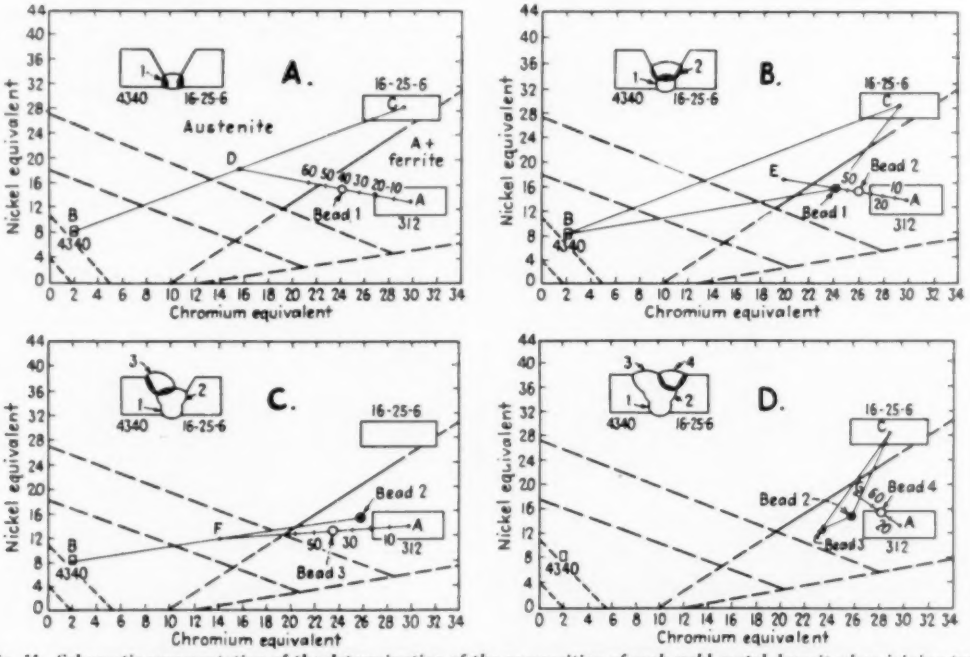


Fig. 11 Schematic representation of the determination of the composition of each weld-metal deposit when joining two dissimilar metals by multipass welding[48]

steel from the clad side after the mild steel side has been welded.[105] In fillet welds dilution is also believed to be relatively low.[104]

According to Schaeffler[105] the chilling effect of the groove design is the most important factor; i.e., dilution depends upon the "angle of effective cooling." The word "effective" is primarily related to the relative quenching effect of the base metal area. Thus a thin plate or sheet would cause less "effective" cooling than a thick plate. For a bead-on-plate weld, the "angle" of cooling is roughly 180 degrees and dilution averages about 30 to 35%. In bead-on-bead welding the "angle" of cooling decreases to about 90 to 120 degrees which corresponds to dilutions ranging from 40 to 50%, as determined by actual measurements. On the other hand, in the root pass on a backed-up groove, cooling is partly upward as well as sideways and down and the angle of cooling approaches 250 degrees. Dilutions of around 10 to 15% have been determined for these welds.[105]

Ordinarily, penetration will be substantially less in vertical welds than in flat welds or overhead welds.[58]

When, in deep penetration welding, a relatively large quantity of base steel is molten and dilutes the weld metal sufficiently to cause the formation of martensite, the use of electrodes of higher alloy content is often desirable. Thus, when severe dilution of 18–8 (Cr-Ni) would produce a weld metal containing considerable quantities of martensite, such undesirable martensite formation can usually be eliminated or minimized by welding with 25–20 (Cr-Ni) electrodes. However, sometimes even the 25–20 (Cr-Ni) weld metal may be diluted to such an extent that it becomes partially martensitic. For example, weld metal produced by submerged arc welding will usually exhibit dilutions ranging from 40 to 75%.[58] Because of the high dilutions many welders find that 25–20 (Cr-Ni) electrodes are unsuitable for this process[98] unless special procedures are used which reduce dilution to below 40 to 50%.*

* The higher 60% value in Fig. 7 pertains to dilutions between 25–20 (Cr-Ni) electrode metal and a nickel-bearing alloy steel.

A method which has often produced satisfactory results on relatively thick plates (over 1 in.) consists of buttering the groove faces of the base steel with 25–20 (Cr-Ni) weld metal similar to the procedure shown in Fig. 12.[84, 85, 101] The balance of the weld may then be completed with either 25–20 or 18–8 (Cr-Ni) weld metal.* For example, Sykes[101] produced good results in manual arc welding of $1^3/_4$-in. thick mild steel plates by buttering the groove faces of a double V-joint with 25–20 (Cr-Ni) electrodes (0.16 in. diameter) and completing the balance of the weld with 18–8 (Cr-Ni) electrodes (0.23 in. diameter). Vickers hardness determinations (30-kg. diamond) gave the following results:[101]

Mild steel	178
Mild steel—25–20 (Cr-Ni) weld-metal transition zone	192–204
25–20 (Cr-Ni) weld metal	234
25–20 (Cr-Ni)—18–8 (Cr-Ni) weld-metal transition zone	215
18–8 (Cr-Ni) weld metal	227

Buttering with 18–8 (Cr-Ni) electrodes (0.23 in. diameter) gave Vickers hardness values of 402 in the buttered layer.[101] Buttering with 18–8 (Cr-Ni) electrodes of smaller diameter reduced dilution. For example, buttering with 18–8 (Cr-Ni) electrodes of 0.13 in. diameter resulted in a hardness of 301.[101]

The importance of avoiding martensite formation in the weld metal is often exaggerated, especially in those cases where softening will occur during service.[101] Thus, in tensile tests which Sykes[101] made across the joints of the above specimens, fracture occurred in the martensitic buttered deposit only in the case of the 18–8 (Cr-Ni) weldment which gave hardness values of 402. Nevertheless, martensitic fusion zones are likely to constitute a weakness during welding.[101] Attention should also be drawn to the advisability of avoiding large diameter electrodes in highly restrained dissimilar-metal welds, not only because they increase the dilutions but also because they result in greater shrinkage stresses across the joint.[101] This may not apply in cases where low restraint is present during welding or

* See also page 55-s.

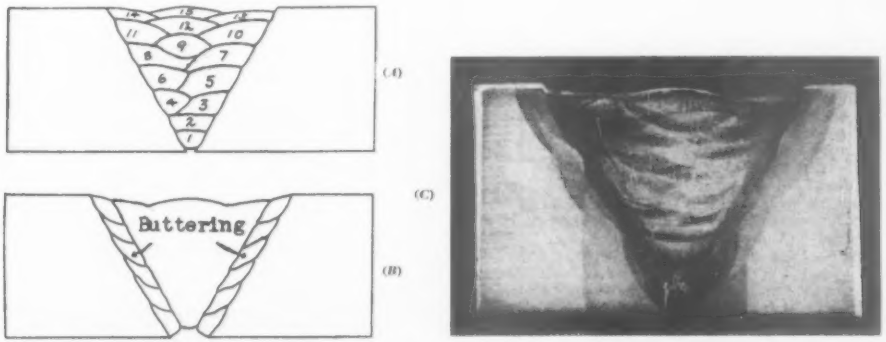


Fig. 12 Welding procedure for a dissimilar-metal joint having a high restraint. (A) Order in which weld beads are laid; (B) method of buttering groove faces; and (C) multibead 18–8 (Cr-Ni) weld buttered with 25–20 (Cr-Ni) electrode metal[84]

where a satisfactory buttering technique with 25–20 (Cr-Ni) electrodes has been used.[101]

One objection to the use of a 25–20 (Cr-Ni) electrode for these dissimilar-metal welds is that a fully austenitic deposit of this type is generally susceptible to hot-cracking—particularly under restraint.[2] However, this does not apply where the dilutions introduce a sufficient proportion of high-temperature ferrite (forming above the M_s temperature) without martensite hardening.[101] For example, in the welding of 20% chromium stainless-steel sheets or thin-walled tubes, where dilutions are about 40%, 25–20 (Cr-Ni) electrodes produce a deposit containing 23% chromium and 12% nickel which has up to 10% of high-temperature ferrite and does not harden up.[101]

Factors Minimizing Dilution

In dissimilar-metal welding, dilution is minimized by the use of low welding currents*[24, 94] and relatively high arc voltages.[24] When these conditions are met, dilution in single-layer deposits made, for example, by submerged-arc welding may be reduced to values as low as 20%; that is, 20% by volume of the fused metal consists of base-plate composition and 80% of (undiluted) weld-metal composition.[24]

In metal arc welding an increase in the speed of electrode travel generally tends to increase dilution. Although increased welding speed decreases the amount of base metal that is molten, the quantity of deposited electrode metal is decreased even more so that the ratio of molten base steel to deposited electrode metal will increase. These relations are apparent from arc-welding experiments by Okada and Ikawa[102] shown in Figs. 13 and 14. A single bead from an 18–8 (Cr-Ni) electrode was deposited on a 0.2-in. thick mild steel plate. The welding current was 125 amp. and the arc voltage was 25 v., d. c., reverse polarity. Data of similar experiments on a 0.6-in. thick plate are also indicated.[102] Results of hardness determinations of the weld metals are given in Fig. 15.

Dilution will also be affected by welding technique and welding sequence.

Data on the effects of electrode diameter have not yet been published. It can be argued that even though a larger electrode will "dig deeper" it also deposits

* The fact that higher welding currents increase dilution is in good accord with predictions based on the theory of moving heat sources. The amount of the base metal participating in the molten metal pool increases more rapidly with the current than the amount of the metal deposited from the electrode.[25]

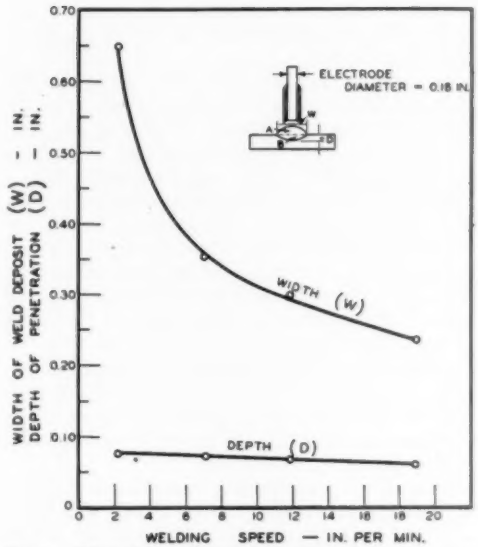


Fig. 14 Effect of welding speed upon width of weld deposit and depth of penetration[102]

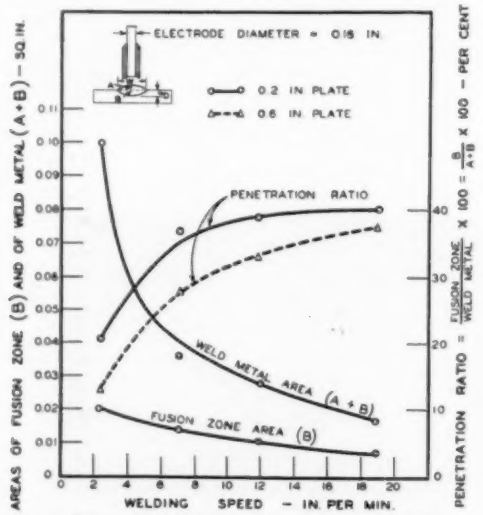

ELECTRODE DIAMETER = 0.18 IN.
0.2 IN. PLATE
0.6 IN. PLATE
PENETRATION RATIO
WELD METAL AREA (A + B)
FUSION ZONE AREA (B)
AREAS OF FUSION ZONE (B) AND OF WELD METAL (A+B) — SQ. IN.
PENETRATION RATIO = FUSION ZONE / WELD METAL X 100 = B/A+B X 100 — PER CENT
WELDING SPEED — IN. PER MIN.

Fig. 13 Effect of welding speed upon fusion zone and weld-metal areas[102]

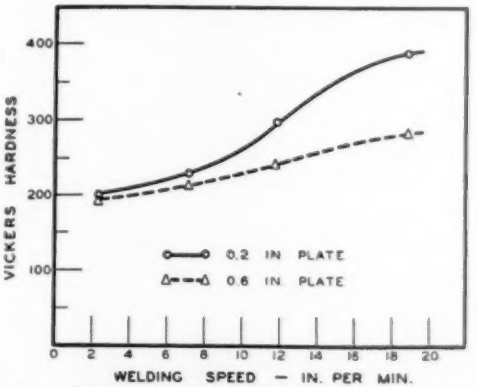


Fig. 15 Effect of welding speed upon weld-metal hardness[102]

more filler metal and hence may not have any effect on the total dilution.[105] In other words, if the current density is the same for all sizes of electrodes, there should be no change in dilution.[105] Penetration will be greater, but not the dilution.[105]

Dilution may be reduced somewhat by designing the joint so that penetration or melting of the base steel is minimized.[29]

In *submerged-arc welding* dilution is relatively high because of the high current density used.[105] However, arc voltage and travel speed represent also important variables. That dilution is increased as welding current, arc voltage, and travel speed are increased is well illustrated by results by Kerr[103] which are given in Tables 3 to 5.

The submerged-arc process[104] may be modified by employing two electrodes. If they are electrically connected in series the penetrating effect of the welding arc is considerably reduced. Consequently, a smaller amount of base metal is fused with the weld metal. Thus dilution may be reduced to values below 10% and in some cases may even be as low as 1.5%.[24]

The *inert-gas metal-arc welding process* is also believed[36] to produce low dilutions. Particularly low values have been obtained with the so-called Aircomatic process. However, whereas consuming stainless-steel electrodes are used in the Aircomatic process, non-consuming tungsten electrodes are used in the conventional inert-gas welding process. Although dilution of the initial weld bead has been reduced by careful procedures to below 10%,[36] it usually exceeds this value.[96, 97] Manual procedures usually result in less dilution than automatic procedures because of the lower currents used in manual welding.

Melting of the Base Steel

Melting of the base steel is not necessarily continuous, but may instead proceed along grain boundaries.[85] Thus the action of the arc and the great thermal gradi-

Table 3—Effect of Welding Current on the Welding Characteristics of Submerged-Arc Welds on a Carbon-Steel Plate.[103] (Type 430 Electrode Wire, 30 V., d. c., Reversed Polarity, 20 In. per Minute Travel Speed, Linde 80 Flux)

Welding current, amp.	*Deposit width, in.*	*Crown height, in.*	*Penetration, in.*	*Base area, sq. in.*	*Crown area, sq. in.*	*Dilution, %*
250	0.410	0.074	0.057	0.0129	0.0212	37.6
300	0.506	0.081	0.084	0.0230	0.0290	44.2
350	0.573	0.082	0.082	0.0269	0.0331	44.8
400	0.585	0.089	0.117	0.0371	0.0390	48.7
450	0.620	0.106	0.153	0.0532	0.0471	53.0
500	0.666	0.126	0.164	0.0632	0.0546	53.7
550	0.670	0.163	0.225	0.0855	0.0620	58.0
600	0.659	0.162	0.299	0.1094	0.0699	61.0
650	0.644	0.188	0.346	0.1329	0.0794	62.4

Table 4—Effect of Arc Voltage on the Welding Characteristics of Submerged-Arc Welds on a Carbon-Steel Plate.[103] (Type 430 Electrode Wire, 400 Amp., d. c., Reversed Polarity, 20 In. per Minute Travel Speed, Linde 80 Flux)

Welding voltage, v.	*Deposit width, in.*	*Crown height, in.*	*Penetration, in.*	*Base area, sq. in.*	*Crown area, sq. in.*	*Dilution, %*
25	0.545	0.119	0.108	0.0350	0.0414	45.8
27½	0.585	0.093	0.120	0.0388	0.0412	48.4
30	0.585	0.089	0.117	0.0371	0.0390	48.7
32½	0.621	0.089	0.136	0.0420	0.0405	50.9
35	0.636	0.081	0.146	0.0487	0.0388	55.6
37½	0.665	0.077	0.134	0.0444	0.0380	53.9
40	0.704	0.081	0.137	0.0503	0.0393	56.1
42½	0.726	0.076	0.143	0.0535	0.0378	58.6
45	0.710	0.079	0.161	0.0600	0.0396	60.3
47½	0.670	0.082	0.165	0.0623	0.0380	62.1

Table 5—Effect of Travel Speed on the Welding Characteristics of Submerged-Arc Welds on a Carbon-Steel Plate.[103] (Type 430 Electrode Wire, 400 Amp., 30 V., d. c., Reversed Polarity, Linde 80 Flux)

Welding speed, in. per minute	*Deposit width, in.*	*Crown height, in.*	*Penetration, in.*	*Base area, sq. in.*	*Crown area, sq. in.*	*Dilution, %*
4	1.200	0.210	0.247	0.1340	0.1997	40.1
8	0.953	0.132	0.177	0.0800	0.0950	45.7
10	0.785	0.131	0.202	0.0790	0.0769	50.6
12	0.807	0.105	0.131	0.0546	0.0621	46.8
16	0.709	0.091	0.143	0.0488	0.0478	50.3
20	0.715	0.140	0.175	0.0371	0.0390	48.7
24	0.573	0.071	0.115	0.0365	0.0321	53.3
28	0.530	0.070	0.109	0.0317	0.0267	54.3
32	0.498	0.065	0.112	0.0313	0.0244	56.1
36	0.491	0.066	0.115	0.0331	0.0231	58.9
40	0.456	0.064	0.123	0.0306	0.0194	61.2
50	0.415	0.056	0.128	0.0319	0.0156	67.1

ents in the crater are believed to break off or tear loose slices or flakes of the base steel, which may become detached at one or both ends from the base steel.[75, 85] An example of this is shown in Fig. 16 where such a flake is embedded in 25–20 (Cr-Ni) weld metal which was deposited on a 13% chromium–0.20% aluminum (Type 405) stainless steel. These flakes will generally dissolve in the weld metal. In some cases they are likely to contribute to the banded structure shown in Fig. 8 which was caused by non-uniform dilution. Occasionally these flakes may not have time to dissolve completely and thus they may appear embedded within the weld metal. These effects have been particularly observed in dissimilar welds on hardenable low-alloy steels[75, 85] and chromium stainless steels where it seems that the austenitic weld metal can preferably penetrate the alloy plate near the grain boundaries. These structures, therefore, aid in the study of the solubility effects at the interface.[75]

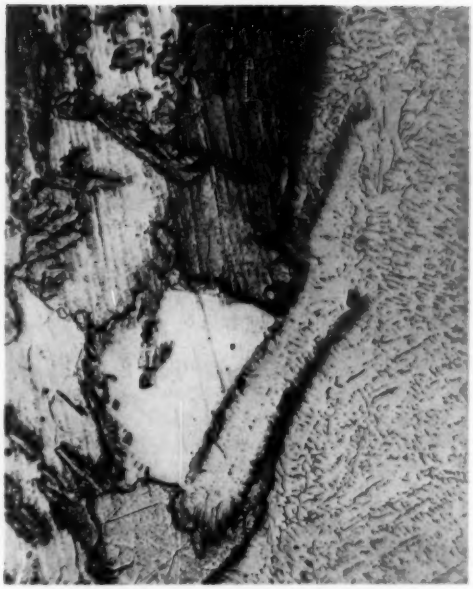


Fig. 16 "Flake" of Type 405 base steel embedded in 25–20 (Cr-Ni) weld metal. Magnification 200 ×

MECHANICAL CHARACTERISTICS

Residual Stresses

The differences in coefficients of expansion in dissimilar-metal joints may introduce residual stresses of considerable magnitude in the joint. Additional stresses are the result of expansion caused by local transformation of austenite to martensite. In addition, the nature of the structure may introduce internal stresses. For example, long pipe sections may cause bending stresses of considerable magnitude.

Restraint will also introduce residual stresses. Thus a tensile stress will develop across a butt weld during cooling if the normal thermal contraction of the weld metal is prevented by external transverse restraint.[87] This latter stress is also called *reaction stress*. However, because of the high plasticity of the austenitic stainless weld metal, the residual stresses are rarely found to be detrimental.

Thus it should be apparent that failures resulting from residual stresses are quite rare. Actually, most failures may be related to weld defects or other structural defects. This, for example, was shown in experiments by Kautz[51] on steel boilers which had been welded with 18–8 (Cr-Ni) electrodes. The vessels were subjected to alternating hydrostatic pressures above normal boiler requirements. In each case, failures in the welded joint could be related to small structural defects or notches which were readily recognizable in radiographic studies.[51]

Only in those conditions, where certain corrosive environments may cause stress-corrosion cracking or caustic embrittlement or where excessive residual stresses may lead to cracking in highly restrained joints of already crack-sensitive weld metals, may it be advisible to reduce residual stresses. These residual stresses may also be thought to be detrimental when actual notches or weld-design notches produce an unfavorable triaxial state of stress in the ferritic steel since this triaxial state of stress raises the transition temperatures from ductile to brittle behavior of the weldment.[33] An example of this was recently reported in the brittle cracking of welded German submarine sections.[74] The cracks primarily occurred in mild-steel weldments. The use of austenitic electrodes overcame this trouble.

Residual stresses may be minimized by preheating the weld joint between 150 and 500° F. (65 and 260° C.).[30] This treatment also tends to produce often beneficial structures in the heat-affected zone of hardenable steels. Special preparation and welding sequences may also be advantageous. Thus although a back-step welding procedure in many cases will produce just as great a residual stress as continuous welding, it is believed[30] that this procedure produces an internal-stress pattern in which the various tension and compression areas are balanced against each other.

Although weaving may produce smaller residual stresses in the longitudinal direction than would be produced by beading,[30, 92] in the transverse direction weaving will produce higher residual stresses than beading.[92] The reason is that in the transverse direction the wider bead deposited by weaving must contract more and the resulting higher residual stresses may cause cracking, particularly when using austenitic electrodes. In any case, when welding with austenitic electrodes the extent of weaving should not exceed two times the nominal diameter of the electrode.[92]

Multibead deposits are believed by some investigators to exhibit lower residual stresses than single bead deposits. This is particularly important in heavy plates since, in general, the residual (transverse) stresses increase with plate thickness.[87] For example, an increase in plate thickness from $^1/_2$ to 1 in. is believed to cause the final stress values to be doubled[87] unless the yield point of the steel is exceeded.

Residual stresses generally are less severe in welded joints produced by automatic welding processes than in those made by manual operations.[20] The accepted reason is that the higher heat input used in automatic welding produces slower rates of solidification and cooling under the slag.

It should be realized that heat treatments cannot effectively relieve residual stresses in weldments composed of materials which have different coefficients of expansion. Mechanical means are more effective in reducing the residual stress level. Thus stress relieving by means of peening of heavy weldments is practiced by many companies. Although excessive peening is likely to cause cracking in the austenitic weld metal,[20] such cracking is not usual since commercial experience teaches the correct amount of peening.

Weld Design

The best American welding practice usually calls for full penetration welding in butt and corner joints. Although butt welds seem to be preferred over fillet welds, the suitability of fillet welds versus butt welds is dependent upon design considerations. In many applications fillet welds are completely adequate.

Distortion

In the production of austenitic weld deposits on armor-plate steels, distortion has been occasionally controlled by shot peening.[29, 30]

Machinability

Weldments which require extensive machining, particularly if a stress-relieving treatment is desired to give machining stability, should be made with ferritic electrodes.

Although austenitic stainless steel is more difficult to machine than ferritic steel, the finished surface of the machined austenitic stainless steel may be superior to the ferritic steel.[6, 106]

EFFECTS OF HEAT TREATMENTS

Stress Relieving

Although in dissimilar-metal joints containing austenitic weld metal the residual stresses may be of considerable magnitude, they are generally not detrimental.[51] Thus stress-relieving heat treatments to remove residual stresses do not seem to be essential. The relatively high plasticity and the absence of notch sensitivity in austenitic weld metal usually allow relief of excessive local stresses by plastic deformation.[51] Furthermore, the difference in thermal expansion coefficients would result in at least part of the residual stresses being restored during cooling from the stress-relieving temperature.

An exception to this can be found in dissimilar metal joints which are exposed to thermal[25] and/or mechanical[112] fatigue or shock. For example, a possible water "carry-over" from boilers into steam piping may result in an instantaneous temperature drop of 400° F. (205° C.) of the welded dissimilar-metal joints in the pipes which are generally made of ferritic chromium-molybdenum alloy steels.[25] With the anticipated high order of stress resulting from differential thermal contraction, it is believed that a postheat treatment to lower the residual stresses and also to reduce the transition temperature from ductile to brittle behavior of the ferritic steel-pipe material would more than offset the detrimental effects of carbide accumulation which may occur during postheating.[109] Thus, in this particular application, postheat treatment of the dissimilar-metal weldment between about 1275 and 1325° F. (690 and 720° C.) has been considered to be beneficial.[109] Stress-relieving treatments are also sometimes used to insure dimensional stability.[92] Ordinarily, however, the detrimental effects resulting from such stress-relieving heat treatments are considered to be greater than the advantages gained from the relief of stresses.

Hydrogen Embrittlement

When moisture may produce hydrogen embrittlement in the heat-affected zone, heat treatments at temperatures between 500 and 700° F. (260 and 370° C.) may sometimes be beneficial if they immediately follow the welding operation. However, when cracks are already present little, if any, improvement may be noted. Ordinarily the use of dry stainless-steel electrodes will prevent this type of embrittlement.

Metallurgical Changes

In dissimilar-metal joints on hardenable steels, where postheat treatments may be necessary to produce beneficial structural changes, the austenitic chromium-nickel stainless-steel weld metal generally requires higher temperatures than those recommended for most of the alloy steels. Thus in many dissimilar-metal joints a suitable heat treatment for one component may have detrimental affects on the other.[42] For example, the austenitic stainless alloys which are susceptible to intergranular carbide precipitation and sigma-phase formation should not be heat treated at temperatures between about 1000 and 1600° F. (540 and 870° C.) and for exposure periods which produce intergranular carbides[2] and/or sigma particles[2, 3]

when the formation of such phases affects detrimentally the life of a particular composition in a certain service environment.

Normalizing

In austenitic stainless weld deposits on mild steels, normalizing of the dissimilar-metal weldment generally is not necessary[52] and, in fact, is often undesirable.[96]

Preheat Treatments

Preheating is often used in situations where the effect of the preheat treatment is not fully understood.[62] The preheating temperature employed prior to welding generally depends upon the particular steel that is to be welded. This is important since the desired effects of the preheat treatment are primarily metallurgical in nature. Thus, increased preheating temperatures have an important effect upon the transformation structures which are produced in the steel as a result of the welding operation.[62] However, not only the composition of the material but also the type of structure comprising the weldment must be considered when a preheat treatment is employed with the sole idea of minimizing stresses. Two alloys which have the same composition but different metallurgical structures, in one case, may be affected beneficially and, in the other, detrimentally.[62]

One of the advantages gained from the use of stainless steel electrodes in welding alloy steels, which ordinarily require preheat treatments, is that when stainless-steel electrodes are used the required preheating temperatures may be reduced by about 200 to 300° F. (110 to 165° C.) below the temperatures recommended for welding with the standard alloy-steel electrodes.

Often preheat treatments are not at all used since sufficient heat is provided by the welding operation itself on structures where the joint does not offer severe restraint. Nevertheless, the following preheating (and interpass) temperatures are ordinarily recommended when welding some of the more common alloy steels with austenitic stainless-steel electrodes:[25, 24, 81, 93, 113]

Material	*Preheat temperature*	
	° *F.*	° *C.*
½Cr – ½Mo	100–300	40–150
1Cr – ½Mo	100–300	40–150
2¼Cr – 1Mo	200–400	95–205
4 to 6Cr – 1Mo	300–500	150–260
7 to 13 Cr	300–600	150–315
3Ni – 1Cr	400–600	205–315

The higher temperature values usually apply to heavier sections exhibiting relatively severe conditions of restraint.

For a preheat treatment to be beneficial, in so far as stresses are concerned, preheating must reduce weld shrinkage.[62] However, preheating can only reduce weld shrinkage if the volume into which the weld metal is deposited is smaller at room temperature than at the preheating temperature.[62]

Three general types of weldments are most commonly used:[62] (1) Two plates are butt-welded together with varying degrees of restraint at the ends and sides; or (2) a homogeneous structure is (repair) welded because it contains an opening or defect (of the type commonly associated with castings); or (3) two pipe sections are butt welded together with varying degrees of restraint at the ends.

The effect of preheating upon each of these types of weldments is different. In the first type, if the plates are restrained at the sides and if the groove face is heated, there will be a tendency for the groove opening to close and the groove face to lengthen. The ratio of the volume of the weld groove at room temperature as compared to the volume at the preheating temperature will depend upon the length of the groove face and the width of the area to be heated.[62] Generally the length of the groove face is long as compared to the width of the area preheated so the effect of the preheat treatment would be to increase the length of the groove to a greater degree than to close the width of the groove opening. This should result in a volume which is greater at the preheat temperature than at room temperature. If the plates are restrained at the ends as well as at the sides, the lengthwise expansion of the groove face will be restricted and closing of the groove opening will be accentuated upon heating. This will tend to produce a smaller volume at the preheat temperature than at room temperature which would tend to increase weld shrinkage and produce higher residual stresses.[62]

In the second type of weldment, if the entire structure is preheated, the opening or weld groove will tend to increase in size producing a volume of weld groove which is larger at the preheating temperature than at room temperature, with a resulting decrease in weld shrinkage and residual stress.[62]

In the third type of weldment, preheating the weld groove in two restrained sections of pipe will lead to a closing of the groove opening and an expansion of the circumference.[62] The ratio of the increase in circumference to the decrease in the weld-groove opening will determine the change in volume upon preheating. If the length of the pipe which is heated is longer than the circumference, it is possible to decrease the volume of the weld groove upon heating with a resulting increase in weld shrinkage and stresses upon subsequent cooling to room temperature. Generally, however, only the face of the weld groove is preheated. This results in a greater increase in circumference as compared to the decrease in groove opening, with a subsequent decrease in weld shrinkage and stresses.[62]

EFFECTS OF ELEVATED-TEMPERATURE EXPOSURE

Carbon Diffusion

Noticeable carbon migration may occur across the bond whenever any two ferrous alloys of different

chemical composition are intimately joined together and are exposed to temperatures at which the diffusion rates of carbon in steel are appreciable. When considerable carbon migration has taken place from the ferritic base steel into the stainless-steel weld metal the strength of a dissimilar-metal weldment may be noticeably reduced.[40] The concentration of the carbide-forming elements in the austenitic stainless-steel weld metal, such as chromium, columbium, titanium, etc., sets up a carbon gradient by the precipitation of carbides in the stainless steel adjacent to the bond. At suitable temperatures this results in further diffusion of carbon from the ferritic base steel into the stainless-steel weld metal of lower "free" carbon content.[101] The welding process increases the propensity for carbon diffusion by retaining in solution carbon atoms in the base steel adjacent to the bond. Upon further heating these "free" carbon atoms are available for migration.

The effects of carbon migration from a 7Cr–$^1/_2$Mo base steel into a 25–20 (Cr-Ni) weld deposit after holding 1000 hr. at 1200° F. (650° C.) are illustrated in Fig. 17. The layer of coarse grains on the ferritic side of the interface is indicative of the decarburization that has occurred.[66]

A microhardness survey across the bond between a 25–20 (Cr-Ni) weld deposit on a $2^1/_4$ chromium alloy steel which had been exposed for 48 hrs. at 1300° F. (705° C.) is shown in Fig. 18.[54] The high hardness values at the bond are believed to be caused by excessive carbide precipitation.[54]

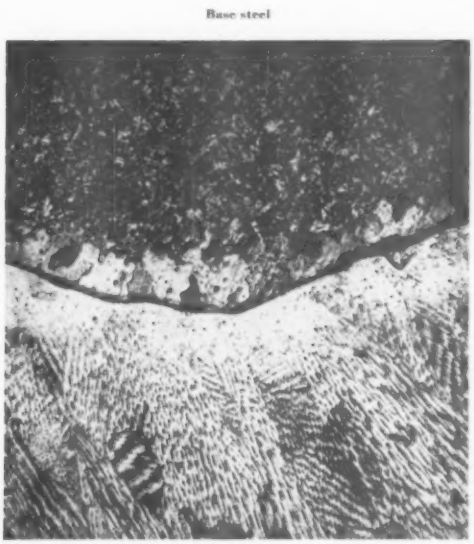


Fig. 17 25–20 (Cr-Ni) weld metal on a 7Cr–$^1/_2$Mo alloy-steel pipe after holding 1000 hr. at 1200° F. (650° C.). The layer of coarse grains on the ferritic base steel side of the bond is indicative of the decarburization that has occurred.[66] Magnification 75 ×. Etchant: mixed acids in glycerol

There are six major factors which influence the rate and degree of carbon migration:[40] (1) welding procedure, (2) temperature of heat treatment, (3) metallurgical structure, (4) alloy composition, (5) time at temperature, and (6) stress.

Welding Procedure

The welding procedure is of importance because it affects the amount of heating of the base steel and thus the width of the area in the heat-affected zone in which carbon is retained in solution. The width of this zone is increased with increases in the amount of heating. Carbon tied up in the form of carbides may also migrate but it must first dissolve in the steel matrix. This is a relatively slow process in ferritic steels.[40]

Temperature of Heat Treatment

The temperature at which carbon migration becomes noticeable depends upon the materials involved and upon the time of exposure at the respective temperature.[40] Between a $2^1/_4$Cr–1Mo steel and a 19–9–Cb (Cr-Ni-Cb) stainless steel the minimum temperature at which noticeable carbon migration occurs is at about 1050° F. (565° C.).

The rate of carbon migration across the bond increases with temperature and approaches a maximum at the lower critical point, i.e., when the ferritic steel begins to transform into austenite. Considerably less migration across the bond occurs above the critical temperature where the ferritic member of the joint has begun to transform into austenite. For example, carbon migration could not be detected in a dissimilar-metal joint consisting of a $2^1/_4$Cr–1Mo base steel and an 18–8–Cb (Cr-Ni-Cb) weld deposit after the joint had been heated at 1650° F. (900° C.) for 1 hr. and quenched in oil.[40] At still higher temperatures, where both members of the weld joint have the same matrix phase, the rate of diffusion across the bond is primarily a function of composition and temperature.[40] Thus after 1 hr. at 1900° F. (1040° C.) the above-mentioned dissimilar-metal joint would be likely to show again noticeable carbon migration from the $2^1/_4$Cr–1Mo steel to the 18–8–Cb (Cr-Ni-Cb) austenitic stainless-steel weld deposit.

Metallurgical Structure

As noted above, the matrix structure exerts a considerable influence upon the rate of carbon migration.[40] When a weldment consists of two different metallurgical structures such as austenite and martensite or ferrite, the rate of carbon diffusion is more rapid than if two ferritic members or two austenitic members were joined together.[40] The reason is believed to be that carbon is considerably more soluble in austenite than in ferrite and that the precipitation of carbides in the

stainless steel sets up a carbon gradient. Thus, when a weldment containing austenite and ferrite is heated in a temperature range in which carbon will diffuse through the ferrite, this greater solubility of carbon in austenite, the lower "free" carbon content in the austenite, and the greater amount of carbide forming elements in the austenite draw the carbon to the bond of the dissimilar-metal weld.* From this point the carbon may diffuse into the austenitic alloy, usually by way of the grain boundaries.[40]

Although the most rapid rates of migration are experienced when the members of the weldment are of different phase structure, carbon will migrate between materials of the same structure but of different composition.[40]

Chemical Composition

Chemical composition is another factor which influences the migration of carbon. Its effects depend upon carbon solubility, carbide formation and stability, and the diffusibility of carbon in austenite and ferrite.[40]

* When the "free" carbon content and the presence of carbide-forming elements is greater in the ferrite phase, carbon tends to diffuse from the austenite to the ferrite areas. Thus, in austenitic stainless steels containing high-temperature ferrite carbon diffuses from the austenite to the chromium-enriched ferrite patches.[94,101]

The carbon solubility of an austenitic weld metal varies with alloy content. For example, nickel tends to lower the solubility and diffusibility of carbon in alloy steels. Thus, in a $2^1/_4$Cr–1Mo steel welded with a 25–20 (Cr-Ni) electrode, the rate of migration in the weld metal is less than in a similar weld made with a 25–12 (Cr-Ni) electrode and given the same heat treatment.[40]

The migration of carbon can also be minimized or even eliminated by the addition of carbide-stabilizing elements such as Cb, Ti, and V, to the ferritic steel.[40] Another method which reduces carbon diffusion is provided by the addition of manganese or nickel to the ferritic steel.[40] These elements appear to decrease the ability of carbon to diffuse through the ferrite phase.

In a weldment with two ferritic members, carbon will migrate if the materials have a different carbon content and are not balanced with respect to carbide-forming elements. Thus carbon diffusion generally occurs from the alloy of lower chromium content to the one of higher chromium content. Some examples obtained by Wylie[40] are given in Table 6.

The addition of elements such as silicon, nickel, and manganese to the weld metals listed in Table 6 may re-

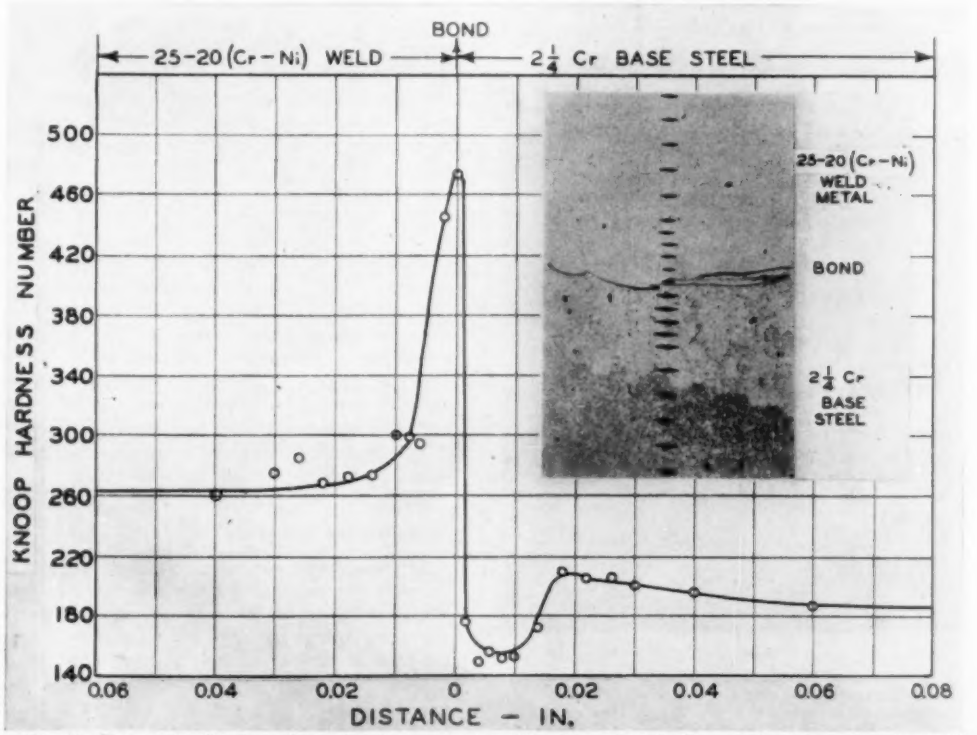


Fig. 18 Knoop microhardness determinations across transition zone of dissimilar-metal weld specimen heated for 48 hr. at 1300° F. (705° C.)[51]

Table 6— Direction of Carbon Migration in Dissimilar-Metal Joints Between Ferritic Alloy Steels[40]

Weld metal	*Carbon migration direction*	*Base material*
5Cr – ½Mo	←	2¼Cr – 1Mo
2¼Cr – 1Mo (0.03 C)	←	2¼Cr – 1Mo (0.08 C)
7Cr – ½Mo	←	2¼Cr – 1Mo
9Cr – 1Mo	←	2¼Cr – 1Mo
12 Cr	←	2¼Cr – 1Mo
16 Cr	←	2¼Cr – 1Mo
28 Cr	←	2¼Cr – 1Mo
2¼Cr – 1Mo (Cb)	←	2¼Cr – 1Mo
2¼Cr – 3Mo	←	2¼Cr – 1Mo
2¼Cr – 1Mo (W)	←	2¼Cr – 1Mo
2¼Cr – 1Mo (V)	←	2¼Cr – 1Mo
18–8 (Cr–Ni)	←	2¼Cr – 1Mo
25–12 (Cr–Ni)	←	2¼Cr – 1Mo
25–20 (Cr–Ni)	←	2¼Cr – 1Mo

duce the tendency for and the degree of carbon migration by decreasing the solubility of carbon in the ferrite and, possibly, by decreasing the rate of diffusion.[40]

As already mentioned, carbon migration can also occur between two austenitic materials of different chemical analyses.[40] For example, an 18–8–Cb (Cr-Ni-Cb) clad carbon steel* was heated at 1900° F. (1040° C.) for 1 hr. and cooled rapidly. At this temperature both the stainless cladding and the carbon base steel were austenitic. Considerable migration occurred into the 18–8–Cb (Cr-Ni-Cb) cladding.[40]

Effect of Time at Temperature

Time at temperature is another factor. At 1050° F. (565° C.) noticeable carbon migration will occur in 500 to 1000 hr. from a 2¼Cr–1Mo base steel to an 18–8–Cb (Cr-Ni-Cb) weld metal. At 1350° F. (730° C.) carbon migration in the same joint will be as severe in as little as 10 to 15 minutes.[40] However, the resulting carbide distribution on the stainless steel side of the bond will vary. Thus, at the lower temperatures very little carbon will diffuse within the austenite so that a heavy band of carbides is present at the bond. At 1350° F. (730° C.) the rate of diffusion of carbon within the austenite is nearly equal to the rate of diffusion of carbon from the ferrite across the bond and a considerably smaller quantity of carbides is formed at the bond. Suitable etching procedures show readily that diffusion within the austenite has primarily occurred into the steel along the grain boundaries.[40]

At the temperatures where the rate of migration from the ferrite across the bond exceeds the rate of diffusion of carbon within the austenite, for example, at 1100° F. (595° C.) from a 2¼Cr–1Mo base steel into an 18–8–Cb (Cr-Ni-Cb) weld deposit, an equilibrium carbon content is eventually reached at the bond. On further heating it is believed that sufficient carbon will diffuse from the unaffected ferritic base steel into the carbon depleted zone of the base steel so that eventually there may be no discernible depleted zone in the ferritic steel.[40]

* More detailed results on carbon diffusion in clad stainless steels are given in a subsequent review.[3]

Stress

Stress at temperature also effects the rate of carbon migration. Although difficult to evaluate, the effect of stress has been observed in the amount of migration which has occurred in rotating-beam fatigue specimens as compared with results on unstressed specimens heated in an electric furnace.[40] It is well known that stress accelerates other diffusion phenomena such as carbide precipitation and sigma phase formation, so it should be expected that the same action would occur in carbon migration.[40]

Carbide Precipitation

Carbon diffusion into the stainless weld metal favors intergranular carbide precipitation. In unstabilized grades chromium carbides are primarily formed, in the stabilized alloys columbium, tantalum, or titanium carbides are formed. Although this carbide precipitation lowers the strength of the stainless-steel weldment at the dissimilar-metal joint, it leads to actual failure only in severe service applications. However, when cracks have formed along the bond between the base steel and the stainless weld metal, cracks may continue intergranularly into the stainless weld metal if intergranular carbides have weakened the weld metal. This seems to be particularly true in 18–8 (Cr-Ni) weld metal.

In the 19–9–Cb (Cr-Ni-Cb) alloys carbon migration and subsequent precipitation of columbium carbides is sometimes responsible for a wide band of martensite[26] along the bond. This may be due to an unbalancing of the stability of the austenite.

Shot peening has been found to increase the propensity to failure in 25–20 (Cr-Ni) welds in which chromium carbides had been precipitated.[23]

Graphitization

Recent studies by various investigators have shown that certain types of mild and low-alloy steels, during arc welding, may form a narrow band of unstable carbide. This band usually appears at the extremity of the heat-affected zone in the region where the temperature resulting from the welding operation reached approximately 1300 to 1400° F. (705 to 760° C.).[22] During prolonged heating between 800 to 1200° F. (425 to 650° C.), the unstable carbide may decompose to form graphite. The graphite, when it appears in a chain-like pattern, produces a zone of weakness through the joint which may result in a rupture of the joint.[22] On the other hand, well-dispersed individual graphite particles are less detrimental[71] and, in the absence of other factors, have not been responsible for actual failures. However, when stresses are present, the presence of dispersed graphite particles is likely to reduce the stress-to-rupture strength of the dissimilar metal joint.

Graphitization in dispersed or in chain-like patterns has been most commonly observed in aluminum-killed

mild steels and in aluminum-killed carbon-molybdenum alloy steels.[22]

Some authors believe that chain graphitization in the heat-affected zone of susceptible steels may be prevented by the use of austenitic stainless-steel electrodes.[22] Thus they feel that the beneficial effects of the austenitic weld metal are due to its tendency to absorb carbon from the heat-affected zone of the base steel.[22] This, however, seems doubtful since the weld metal is at a considerable distance from the zone of graphitization.

Studies on the nature and effects of graphitization which are now being conducted in various research laboratories should provide a great deal of additional information on the effects of the different forms of graphite upon the properties of dissimilar-metal joints.

FAILURES IN DISSIMILAR-METAL JOINTS

In most applications dissimilar-metal weldments have given satisfactory service. Cases of failure are few and are not always well documented so that there is much speculation as to the quality of the original weld deposits.[101] Many weldments which may appear satisfactory in a superficial examination contain small defects and microcracks which may be opened up and propagated in service by thermal or mechanical fatigue stresses. Sykes,[101] for example, found that in some laboratory thermal-fatigue tests, where there was no externally applied load, some internal defects opened to the surface after the first 100 to 200 cycles of heating to and cooling from about 1110° F. (600° C.). Although the specimens did not actually fail, there was no doubt that more severe testing or service conditions would have resulted in failure which could be attributed to initially unsound welds.[101]

These observations emphasize the importance of a vigorous examination of each weldment. Since weld inspection in the field is generally difficult, it is often preferred practice to weld the dissimilar-metal joint as a separate unit under the best shop conditions so that full inspection can be carried out before installation. Where this is possible, the extra field welding involved may be justified, since this latter welding does not need to be concerned with dissimilar-metal weldments.[101] The necessity of such procedures, of course, would depend upon the required quality of the dissimilar-metal weldment. Obviously, defects in the weld seams of pressure vessels or piping may result in serious failures during service.

Failures or cracks in dissimilar-metal joints may occur either in the base metal or in the weld metal. Cracks which appear in any one of these zones are usually ascribed to one or several different causes such as inherent defects in the material, improper welding technique, hydrogen embrittlement, thermal or mechanical fatigue, intergranular carbide precipitation, stress-corrosion, creep, etc. Failure itself is the result of (1) crack formation and (2) crack propagation.

Cracking, when the result of welding, is usually distinguished as "hot" or "cold" cracking. "Hot" cracking generally occurs at elevated temperatures in the weld metal while it is cooling. "Cold" cracking is most common in the heat-affected zone of the base steel. It usually occurs after the welded section has cooled to atmospheric temperatures and frequently takes hours or days to propagate into visible cracks.

Base-Metal Cracking

Base-metal cracks are those cracks which, as a result of welding, occur either in the unaffected or in the heat-affected zone of the base steel. Cracks in the latter zone are also commonly known as underbead cracks. Base-metal cracking may be due to defects in the base steel itself and/or to certain effects caused by the welding operation. Stresses in the dissimilar-metal joint may further enhance susceptibility to cracking in the heat-affected zone. Base-metal cracking may occur during cooling when the weldment is still at elevated temperatures or after it has cooled down to atmospheric temperatures.

Base-metal cracking in mild steel is rather uncommon. When it occurs it can generally be ascribed to poor steel quality where the stresses produced by the dissimilar-metal joint have caused cracking along the laminations or segregations of non-metallic inclusions in the base steel.[6]

Base-metal cracks are more common in hardenable steels, particularly in the heat-affected zone. When they occur, they are generally initiated by the formation of small cracks in the martensite and are often extended and propagated into major cracks.[86] This type of crack formation is usually referred to as cold cracking since it occurs some time after the welded section has cooled down to atmospheric temperatures.[86, 99] Actually, a two-stage cracking process has been observed.[27, 86] The first stage which on a nickel-chromium-molybdenum steel containing:

C, %	0.32
Mn, %	0.57
Si, %	0.22
Ni, %	3.38
Cr, %	0.65
Mo, %	0.26

developed relatively slowly was followed, after about 10 min., by a rapid, almost instantaneous, extension into a major crack.[27, 86]

The crack sensitivity in hardenable steels may well be related to a combination of factors such as the low ductility of the martensite of the base steel, serious micro- and macrostresses, and the presence of hydrogen.[15, 49, 95–99]

Although a fully martensitic heat-affected zone is often regarded as brittle, in the absence of welding cracks or non-metallic segregations which produce stress concentrations, failure does not necessarily initiate in this high-strength structure, particularly if the carbon content is low. Furthermore, the slight temper-

ing of subsequent beads in multipass welds greatly improves the impact resistance of martensitic structures. Unlike pearlitic and bainitic structures, tempered martensitic structures show no abrupt decrease in toughness as the severity of impact is increased.[6]

Increases in carbon content tend to increase susceptibility to base-metal cracking. Steel with a carbon content above 0.45% is quite susceptible to cracking.[6] However, suitable preheat and postheat treatments may minimize the cracking propensity of such materials by influencing the transformation characteristic of the steel and, possibly, by reducing shrinkage stresses. A high-carbon content in the base steel is also undesirable when the dissimilar-metal joint is heat treated or exposed at temperatures at which carbon migration readily occurs from the base steel into the stainless-steel weld metal.

Generally, cracking in the heat-affected zone (base-metal cracks) of hardenable steel is prevented or minimized by preheating at suitable temperatures the section which is to be welded. The recommended preheating temperatures primarily depend upon the composition of the base steel and, to a lesser extent, on the welding process and procedure. The preheat treatments generally prevent martensite formation by allowing the austenite to transform at a higher temperature and produce such products as lower bainite, upper bainite, ferrite, pearlite, etc.

In welding hardenable base steels, the effects of welding on the heat-affected zone may be somewhat minimized by keeping the heat input to a minimum, that is, by using stringer beads instead of weaving techniques and by maintaining a low-interpass temperature.[6] However, when in welding plates of medium thickness, the deposition of several weld beads becomes preferred practice, the welding procedure shown in Fig. 19 is considerably more successful than welding stringer beads and finishing up at one edge of the groove.[95] In this so-called annealing-bead technique each bead will postheat the previous bead and preheat the subsequent bead. Buttering the groove faces with 25–20 or 25–12 (Cr-Ni) electrodes of small diameter also minimizes underbead cracking because it minimizes the width of the heat-affected zone and tends to temper the martensite produced by each previous deposit. The weld may be completed with suitable stainless-steel electrodes of relatively large diameter. However, in welding heavy sections of hardenable steels, when preheat treatments are possible, it is preferred to use large electrodes, high currents and preheat temperatures between 600 and 900° F. (315 and 480° C.).[18] When preheat treatments are not possible a buttering procedure with small diameter electrodes seems to be favored by most investigators.

Actual failures in the heat-affected zone of the dissimilar-metal joint which occur during service can be generally related to the initial presence of base-metal cracks acting as notches (stress raisers). Mechanical and thermal stresses, fatigue, etc., may propagate these cracks until failure occurs.

However, service failures can also be related to the presence of initial structural weaknesses (not actual cracks) in the dissimilar-metal joints such as a decarburized zone in the ferritic base steel, graphitization, etc. Although these weaknesses are generally not as severe as actual cracks, stresses of sufficiently high magnitude may lead to cracking and failure. In most dissimilar-metal joints these types of heat-affected zone failures are generally associated with service failures. For example, decarburization of part of the heat-affected zone adjacent to the bond becomes effective only after a sufficiently long exposure at elevated temperatures. Such failures may also be observed when a weakened dissimilar-metal joint experiences thermal[4] or mechanical fatigue of such magnitude that cracking is initiated and/or propagated. Thus in service at elevated temperatures above about 1050° F. (565° C.) base-metal failures may occur in the decarburized base metal as a result of stress oxidation and the low stress–rupture properties of this zone.[72, 96]

As long as the welding operation itself has not produced initial cracking Herres and Turkalo[6] believe that heat-affected zone failures under shock loading conditions of welded sections are reduced by increasing the amount of alloying elements which cause hardenability. According to Herres[12] the toughness of a martensitic heat-affected zone, particularly when it has been tempered by subsequent welding passes, is much better than that of a heat-affected zone which contains high-temperature transformation products such as pearlite, upper bainite or acicular ferrite. Depending on the welding conditions and the rate of cooling of the heat-affected zone, it may be necessary to use a base steel of higher alloy content to obtain the desired structure. However, this is only necessary in weldments designed to withstand heavy shock.[12] Generally this procedure is uneconomical[6] and does not represent industrial practice.[96]

Weld-Metal Cracking

Weld-metal cracks are the most common and serious defects which may occur in dissimilar-metal joints between a ferritic base steel and an austenitic stainless-steel weld metal.

It was pointed out earlier that the weld metal consists of a fusion zone and a bead metal zone. Actual distinction between these two zones is not easily made since they blend into each other. Thus, in a single bead deposit uniform dilution may have caused both zones to be essentially of the same composition. Nevertheless, the metallurgical and physical properties of that area of the fusion zone which is adjacent to the bond often differ somewhat from the balance of the weld metal. For example, this relatively narrow area in the stainless-steel weld metal may exhibit a hardened zone caused by alloying, martensite formation, or carbide precipitation. Internal stresses resulting from different thermal expansion characteristics may also cause hardening and affect the properties of the weld metal in this area.

Many investigators believe that weld-metal failures can be prevented by lining (buttering) the groove faces of the base steel with 25–20 (Cr-Ni) weld metal similar to the procedure shown in Fig. 12.[54, 55] The balance of the weld is then completed with either 25–20, 25–12, or 18–8 (Cr-Ni) weld metal, the first being more commonly used. There are many other investigators who feel that this buttering procedure produces weldments which are somewhat inferior to weldments made with other welding alloys and a welding procedure similar to the one shown in Fig. 19.[56] For example, the use of the recently developed 29–9 (Cr-Ni) electrodes has produced dissimilar-metal weldments which have often given excellent performance—particularly in joints exhibiting high restraint.

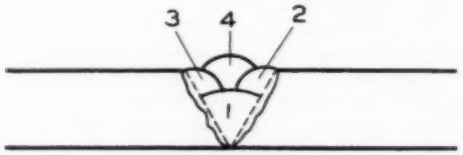


Figure 19

Most weld-metal cracks originate in a narrow area of the fusion zone which is adjacent to the bond. Because cracking may be caused by a number of factors, differentiation may be made among four types of weld-metal failures: (1) bond failures between the fusion zone of the root bead and the heat-affected zone of the base steel, (2) martensite transition-zone cracking, (3) failure in the weld metal just outside the hardened-transition zone, and (4) failure within the bulk of the weld metal.

Bond failures generally are primarily related to improper welding technique or design of the welded joint. Thus slag inclusions, porosity, lack of fusion, etc., may cause poor adhesion between the base steel and weld deposit. Poor design is usually due to a weld joint geometry which leaves a high stress concentration at the junction of the weld metal and the base metal.[6] As Herres and Turkalo[6] pointed out, too narrow a root gap may start bond failures at the root of the weld.

Bond failures are more common in 18–8 (Cr-Ni) multibead weld deposits than in 25–20 or 25–12 (Cr-Ni) weld deposits,[55] particularly on heavy sections.

When *martensite transition-zone cracking* occurs it can generally be ascribed to the nature of the narrow martensitic transition zone in certain types of weld metal adjacent to the bond and to the action of internal transformation stresses, hydrogen embrittlement, and/or shrinkage stresses which are particularly severe in highly restrained joints.[6, 49]

Susceptibility to cracking in this zone may be minimized by (1) minimizing or preventing martensite formation by an increase of the nickel and/or manganese content of the electrode* or by preheating, (2) minimizing the factors which cause shrinkage stresses, and (3) decreasing the moisture content in the electrode coating which, in turn, reduces the amount of hydrogen absorbed by the weld metal and available for embrittlement.[6] Thus by using dry electrodes of suitable composition in welding highly restrained joints, hardened zone cracking is generally avoided. Since the fully austenitic 25–20 (Cr-Ni) weld metal is itself relatively susceptible to hot cracking, it has been preferred practice in welding highly restrained joints on heavy sections of hardenable low-alloy steels to deposit 29–9 or 25–12 (Cr-Ni) weld metal. However, 25–20 (Cr-Ni) electrodes can be considered to be suitable when the dilution is of such an extent that the resulting weld metal will exhibit a partially ferritic structure.

Weld-metal failures just outside the martensite transition zone (when such a zone is present) are usually related to dilution, carbon and other alloy diffusion from the base steel, intergranular carbide precipitation, and other metallurgical changes associated with dissimilar-metal welding and joints. The additional high stresses which may exist in the dissimilar-metal joint and thermal fatigue may cause or propagate cracks along the existing structural inhomogeneities. For example, the crack shown in Fig. 20 was the result of thermal fatigue effects produced by multipass welding of $1^1/_2$-in. hardenable alloy steel plate.[73] Each successive bead produced thermal fatigue stresses which

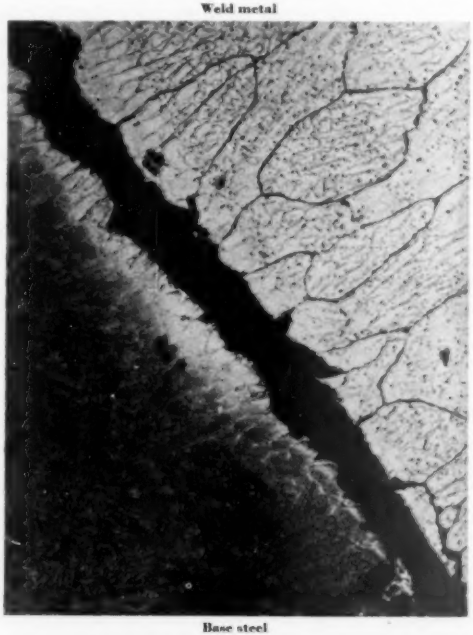


Fig. 20 Weld-metal failure in manganese-bearing 19–9 (Cr-Ni) austenitic stainless-steel weld metal parallel to bond.[73] Magnification 275 ×

* Since electrode compositions are very critical, this statement applied to well-balanced electrodes producing sound weld metal; i.e., weld metal which is not susceptible to hot cracking.

propagated an initial root crack by continuing the crack through the hardened weld metal parallel to the bond. This type of failure is not uncommon to fabricators of heavy weldments where conditions of high restraint exist. The cracking shown in Fig. 20 has been circumvented by the use of special welding techniques such as buttering.[73]

Although these types of failures are relatively uncommon in plates up to a thickness of $^1/_2$ in., they can often be related to intergranular carbide precipitation and to the presence of high triaxial stresses in the weld[85] which may be further aggravated by mechanical or thermal fatigue. The 18–8 (Cr-Ni) deposit on hardenable low-alloy steels seem to be particularly susceptible. Thus tensile test bars through 18–8 (Cr-Ni) welds on hardenable low-alloys steels became quite brittle in the fusion zone of the weld after exposure to temperatures at which carbide precipitation occurred. The results are illustrated in Fig. 21 and show that heating for 10 min. at temperatures between 1380 and 1790° F. (750 and 975° C.) reduced elongation (in 1 in.) from about 40% to about 4 to 10%.* The breaking strength was obtained by dividing the tensile strength (which remained constant at about 78,000 psi.) by the final cross-sectional area of the specimen. Weld deposits made from 25–20 (Cr-Ni) electrodes did not exhibit this type of embrittlement in the fusion zone.[85]

Although the fusion-zone cracks generally originate in the bond, they are sometimes found to deviate sharply into the austenitic weld metal of 18–8 (Cr-Ni) weld deposits on alloy steels where they usually follow the grain boundaries. Small cracks usually branch out from these main cracks. They are also intercrystalline and are believed to be related to intergranular carbide precipitation.[85]

Weld-metal cracking away from the actual fusion zone generally is a characteristic of the austenitic weld metal. The major factors which cause or influence this type of cracking have already been discussed.[2] It should be emphasized however that since, in dissimilar-metal joints, the residual stresses are generally of greater magnitude than they are in similar metal joints, greater care has to be taken to produce satisfactory weld deposits in dissimilar-metal joints. Since fully austenitic weld metal, such as 25–20 (Cr-Ni), is relatively susceptible to hot cracking between 1470 and 2010° F. (800 and 1100° C.), the partially ferritic 29–9 or 25–12 (Cr-Ni) grades are often preferred—particularly in heavy sections which offer high restraint. In Europe fully austenitic 18–8–6 (Cr-Ni-Mn) electrodes have found considerable use since the weld metal from these electrodes is also less susceptible to hot cracking.[107, 108]

The root deposits in butt welds in mild- or alloy-steel base plates are particularly prone to cracking. In these, two types of cracks may be observed. The first consists of a longitudinal type of crack extending from the top of the root bead toward the base of the bead. This crack is most common in partially ferritic deposits but may occur also in fully austenitic weld metal. The second type of crack usually is multidirectional and follows the grain boundaries of the dendrites. It is found in martensitic alloy and chromium stainless-steel weld deposits[15] and in fully austenitic stainless-steel weld deposits.[78] A typical example of the latter type of crack in the root pass of a fully austenitic 25–20 (Cr-Ni) weld made in a 7Cr–0.5Mo steel pipe is illustrated in Fig. 22.[56] Partially ferritic type 18–8 or 25–12 (Cr-Ni) weld deposits do not exhibit these multidirectional cracks.

Weld-metal cracking may be minimized by using electrodes which produce partially ferritic weld metal, by back stepping[20] and by buttering. Preheating may also be beneficial as are slower cooling rates.

HARDENABLE STEELS

The development of low-alloy, high-strength, constructional steels and of low-alloy, armor-plate steels

* Sigma-phase precipitation instead of carbides may actually have been responsible for these failures.[85]

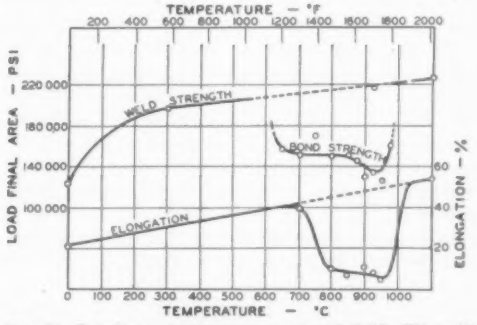


Fig. 21 Brittle-temperature range in 18–8 (Cr-Ni) weld metal[85]

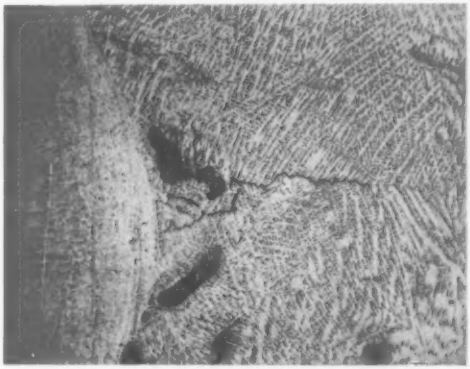


Fig. 22 Intergranular cracking in root deposit from 25–20 (Cr-Ni) stainless-steel electrode made in a 7Cr–$^1/_2$Mo alloy steel pipe.[56] Magnification 25 ×. Etchant: mixed acids in glycerol

in the years preceding the Second World War was responsible for various investigations on the weldability of these materials. Initial studies with different types of low-alloy, high-strength electrodes produced weld deposits which were highly susceptible to cold cracking. The cracks usually occurred in the heat-affected zone or in the bond.[29, 78] According to present hypotheses, most of these failures appear to be related to the hydrogen content in the electrode coatings.[45, 49, 78] The hydrogen seems to act as a contributory factor to other causes such as the presence of multiaxial shrinkage stresses, the formation of hard brittle zones, the austenite-ferrite or austenite-martensite transformation, etc. The elimination of hydrogen greatly reduced the tendency for failure in armor-plate weldments[30, 76] and in other hardenable low-alloy steels.[49]

Until recently, the dangers of embrittlement caused by the presence of hydrogen had been minimized by the use of suitable austenitic stainless-steel electrodes and welding procedures. However, at the present time, low-hydrogen, low-alloy steel electrodes are being developed and produced which are suitable for the welding of many hardenable and armor-plate steels.

Austenitic Electrodes

In the years preceding the Second World War, the hardenable low-alloy and armor-plate steels generally were welded with suitable austenitic stainless electrodes —usually of the 18–8 (Cr-Ni) type.[6, 10, 29, 38, 51–53, 57, 60, 63, 66–67] One of the major advantages of the austenitic electrodes was that the weldments usually did not require preheat or postheat treatments which had generally been necessary when the weldments were made with low-alloy steel electrodes. This may be important, since in the final assembly of armor weldments, preheating is often inadvisable because the welder must work inside the structure.[29]

However, there are additional factors which explain why most of the early low-alloy steel weldments were extremely crack sensitive—even when preheat treatments were employed. The hydrogen hypothesis also seems to offer the major explanation why austenitic stainless electrodes produced better weld deposits on armor plates than the ordinary mild or low-alloy steel electrodes did. The facts, that dry lime-coated electrodes contain a minimum amount of hydrogen[3] and that austenite may safely contain a larger quantity of hydrogen than ferrite or martensite, are the major explanations for the advantages of austenitic deposits.[30] In addition, the presence of large amounts of CO_2 in the arc stream, which preclude the existence of hydrogen, should also be considered.[96]

Since fully austenitic weld deposits from chromium-nickel stainless-steel electrodes are susceptible to root-bead cracking, particularly with the high restraint present in dissimilar-metal weldments, it soon became apparent that for best results some ferrite should be developed* in the austenitic weld-metal structure.[1, 2, 8, 10, 11, 29, 41] Further advantages seem to be obtained by the addition of molybdenum to 18–8 (Cr-Ni) type compositions in which the chromium-nickel content was adjusted to give over 5% ferrite in the as-welded deposit.[29] Molybdenum minimizes hot cracking in the center of the weld bead.

The presence of a few per cent of manganese in the stainless-steel electrode has also been found to reduce crack sensitivity of the weldment. To some extent the beneficial effects of manganese are believed to be due to the fact that manganese minimizes the transformation of austenite which is caused by dilution[8]; i.e., the formation of a hard fusion zone. This may explain why the U. S. Army Specification 57-203-3[91] requires the presence of 1.6 to 4.75% manganese in austenitic stainless-steel electrodes. Nevertheless in highly restrained joints manganese does not inhibit but merely delays the formation of (cold) cracks over a period of time which depends on the minimum temperature reached during that period.[73] Cracking has occurred as late as 30 days after completion of the weld.

Manganese seems to inhibit the sensitivity of undiluted austenitic weld metal to hot cracking. For example, fully austenitic stainless-steel weld deposits, when containing several per cent of manganese, have been found to be fully resistant to hot cracking. For these reasons the Germans used an electrode during World War II to weld their hardenable armor plate steels which contained about 0.10 to 0.20% carbon, 18% chromium, 8% nickel and 6% manganese. Weld deposits from this electrode showed excellent resistance to hot cracking in severe hot-cracking tests.[68] Moreover, all-weld-metal tensile specimens from these 18–8–6 (Cr-Ni-Mn) electrodes showed elongations of about 45%.[108] So far it has not been clearly established why the fully austenitic manganese-bearing stainless-steel weld deposits do not seem to be susceptible to hot cracking, since it is well known that manganese-free fully austenitic weld metal is crack sensitive particularly under highly restrained welding conditions. Kauhausen[108] suggests that the differences may be related to the residual sulphur content present in the weld metal.

In chromium-nickel stainless steels the sulphide tends to form a nickel-sulphide which has a melting point at 1450° F. (790° C.) and, as such, promotes hot cracking.† In the presence of several per cent of manganese, Kauhausen[108] believes that a manganese sulphide is formed which has its melting point at 2930° F. (1610° C.). For this reason several German 25–20 (Cr-Ni) electrode grades are alloyed with up to 2.5% manganese. A higher manganese content would be undesirable because of a tendency to reduce heat resistance.[108]

Dilution and the formation of a hard fusion zone has also been minimized by making an initial 25–20 (Cr-Ni)

* Since in service applications above 900° F. (480° C.) the presence of ferrite enhances sigma phase formation the advantage of the partially ferritic electrodes in weldments for high-temperature service may be questional.[5, 100]

† Other theories proposed to explain hot-cracking in fully austenitic weld metal have been summarized in an earlier review.[3]

deposit and following it by depositing 25–20, 25–12, or 18–8 (Cr-Ni) weld metal. However, since welding with different types of electrodes is undesirable, the use of molybdenum, and/or manganese-bearing, partially ferritic stainless-steel electrodes is generally preferred practice.

Large electrodes are preferred not only because they offer production advantages, but also because weldability is improved.[9, 29] This is primarily due to the increased rate of heat input and the resultant lowering of the cooling rate.[29] Thus $^1/_2$-in. diameter austenitic electrodes have been successfully used in the hand welding of butt welds on cast armor plates.[30] Ordinarily, however, electrodes above a diameter of $^1/_4$ in. are rarely commercially used in hand welding. Electrodes of diameters above $^1/_4$ in. may be used in automatic or semiautomatic welding procedures.

The maximum permissible carbon content of the armor plate, for satisfactory welding with austenitic electrodes, is about 0.35% carbon. Plates with carbon contents above 0.35% and up to 0.46% can only be welded with preheating to 210 to 300° F. (100 to 150° C.) and special techniques.[30, 78] Steels with a higher carbon content were found to be extremely difficult to weld satisfactorily.[76] The other alloying elements are effective in that, generally, the greater the hardenability of the steel the greater is its susceptibility to cracking.[30]

Ballistic tests on austenitic stainless weldments on armor-plate steels showed the stainless weld metal to be of lower strength than the armor-plate steel. In this respect, 25–20 (Cr-Ni) weld metal proved to be weaker than 18–8 (Cr-Ni) weld metal.[82] Thus 18–8 (Cr-Ni) electrodes seem to have been most commonly used in armor-plate weldments.[82] The initial practice of finishing off the stainless weld deposits with a few layers of hard-facing alloys proved to be undesirable because of a high cracking propensity.[82] Instead it became preferred practice to build up the weld reinforcement to a greater thickness than the plate in order to compensate for the relative weakness of the austenitic material.[82]

Welding Procedure

The joint designs, which are most suited to the production of metal-arc welds on a hardenable steel free from defects, are those which provide a sufficiently wide root opening for the first layer so that root fusion may be obtained.[29] Single V- or double V-joints generally are most economical to prepare. For the single V-joint a temporary backing strip is used to provide an adequate root opening, whereas for the double V-joints several types of backing have been successfully used. One of these joints uses a temporary copper backing bar which is usually shaped to the same angle as the groove, but need not be as wide as the root opening.[29] The copper bar acts as a chill for the first layer of weld metal. The weld casts against the bar without fusing to it. Instead of using a copper bar, an 18–8 (Cr-Ni) steel strip $^1/_8$ by $^5/_{16}$ in. in cross section may be welded to the root faces of the double V-joint of each plate.[29] This in turn acts as a backing for the welds. A curved edge is preferable to a square edge on this backing strip, so that the joints between the backing and the plates may be made without leaving continuous lines of "lack of fusion" at these locations.[29] Chipping or grinding is generally not required in these two types of double V-joints. Other procedures may also give satisfactory joints.[29]

Various methods have been successfully used in the deposition of austenitic weld metal on hardenable low-alloy steels. However, best results in welding V- and double V-joints with wide root spacing and backing are obtained by depositing the first layer by a backstepping technique by means of two beads of weld metal, the second bead overlapping the first.[29] In the case of the double V-joint, the first layer on the second side is generally deposited before the subsequent layers on the first side are completed.[29] Aborn, Hiemke, and Stitt[29] also mentioned that the first four passes may be alternated on the two sides of double V-joints. However, in no case should more than four passes be completed on the first side before the initial pass on the second side is deposited.[29] In very heavy sections, a block or pyramid technique seems to be best in order to avoid cracking and to control distortion.[29]

Crater cracks which may form in austenitic weld deposits should be ground out before depositing subsequent beads.[29] Moreover, in the case of an improperly formed bead, it may be necessary to remove a slag line or a line of incomplete fusion at the side wall. Because of martensite formation in the hardenable steel, this removal of the "incomplete fusion line" generally necessitates grinding rather than chipping.[29] Grinding is also required in the case of a double V-weld in which a narrow root opening without backing is employed, in order to remove icicles and slag before depositing weld metal on the second side.[29]

STAINLESS STEELS

Austenitic Stainless Steels

Ordinarily, austenitic-stainless steels are welded with electrodes in which the chromium-nickel content is similar to that of the base metal.[1, 2] In a few instances, however, electrodes have been suggested in which the chromium-nickel content of the austenitic base steel varied considerably from that of the electrode.

Emerson and Morrow,[7] for example, reported detailed results on the use of 29–9–Cb (Cr-Ni-Cb) electrodes in submerged-arc welding of Type 347 19–9–Cb (Cr-Ni-Cb) stainless pipes. This highly alloyed submerged-arc welding wire seems applicable only in welded structures which are used in the as-welded state, particularly when the conditions are such that considerable dilution occurs between the 29–9–Cb weld metal and the 18–8–Cb base steel.[21] Such welds seem to be some-

what less susceptible to cracking[1] than if they had been made with 19–9–Cb electrodes.

Ferritic Chromium Stainless Steels

The ferritic chromium stainless steels are generally welded with austenitic stainless electrodes. The welding metallurgy of these alloys based on studies by Eberle,[43, 45–47] Fowler,[44] and others has already been discussed.[2]

CORROSION

Stainless weldments on boiler-plate steels will not be detrimentally affected in the usual boiler application. Galvanic effects do not have to be considered between ordinary stainless weld metals and the boiler-plate steel.[80]

However, there are other applications where galvanic corrosion must be considered. For example, in a paper pulp digester lined with Type 410 stainless steel and welded with 25–20 (Cr-Ni) electrodes the 12% chromium liner was badly corroded at the bond adjacent to the weld deposit.[96] Similarly, low-carbon ferritic 13 and 17% chromium stainless steels have become susceptible to intergranular corrosion in the heat-affected zone of the base steel after welding with austenitic stainless steel electrodes and in the absence of postheat treatments.[110] After a postheat treatment at about 1380° F. (750° C.) the intergranular corrosion does not occur. Instead, severely accelerated corrosion may occur at the bond. According to Baerlecken,[110] this susceptibility of welded ferritic chromium stainless steels to intergranular corrosion by certain solutions has been avoided by the use of a titanium stabilized chromium stainless base steel which does not require a postheat treatment. Unfortunately, such a steel exhibits severe grain growth in the heat-affected zone and low ductility.[110]

When non-stabilized chromium stainless steels are to be welded with austenitic stainless-steel electrodes it may be necessary to line that side of the weld which is exposed to the corrosive environment with a final deposit from a suitable chromium stainless-steel electrode.[110] However, such a joint generally requires a postheat treatment of 1 hr. at about 1350° F. (730° C.)

Since galvanic corrosion depends upon the corrosive medium, the service environment to which the dissimilar-metal joint is exposed should be considered.

APPLICATIONS

Dissimilar-metal joints play an important part in the fabrication of many different types of industrial and military equipment. Thus accurate information on the welding and service characteristics of dissimilar-metal joints is of considerable interest to many manufacturers and users. In many instances, the useful service obtained from dissimilar-metal joints has been due not so much to a metallurgically correct design, as to the fact that service conditions were not severe enough to result in failure. In fact, many joints have operated satisfactorily for as long as ten years before failure actually occurred. At the time when these joints were welded, no such failures were anticipated.[96]

The metallurgy of welding mild, low-, or high-alloy steels with austenitic stainless electrodes and, in particular, the effects of electrode composition, coating technique, microstructure, etc., on the service performance of dissimilar-metal weldments is not always understood. Moreover, the type of service to which the joint is to be subjected may have much to do with its satisfactory performance. This last factor is sometimes neglected.

Each application involving dissimilar-metal joints requires individual study if failure in service is to be avoided. For example, differentiation should be made between weldments operating at normal atmospheric temperatures and those operating at elevated temperatures. Thus mild steels have been welded satisfactorily to the austenitic stainless steels with stainless-steel electrodes for room or slightly elevated temperature service without any difficulty.[96] Even with metallurgically improperly balanced electrodes, fissured weld deposits, and possibly improper welding technique, such joints have often performed satisfactorily in room-temperature service. Structural repairs on machinery frames, etc., are a good example of this type of dissimilar-metal joint.[96] On the other hand, depending on the temperature and the loading,[96] a joint, which is to operate under stress at temperatures above 900° F. (480° C.), may quickly fail.

The characteristics of different electrode materials, welding processes, and procedures should also be considered. The depositing of brittle weldments and the formation of fissures in the weld deposit should be avoided. These difficulties often increase with the use of automatic welding procedures used in production.[83]

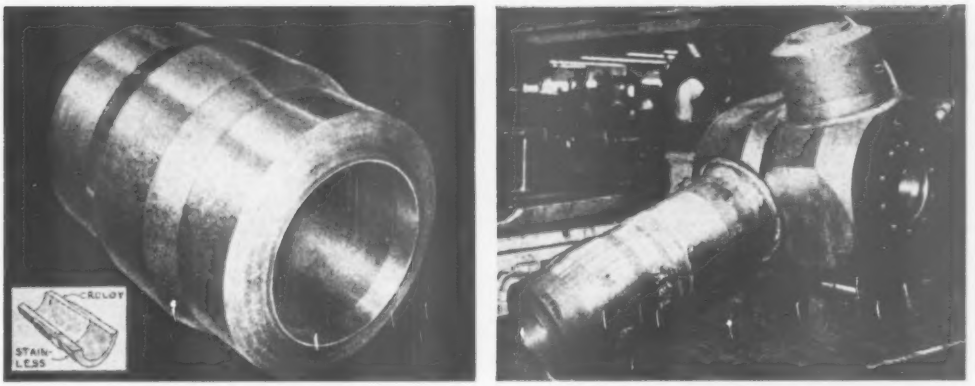
SPECIAL JOINTS

Although standard welding procedures are generally used to join dissimilar metals, a few special types of joints have been produced commercially which are not directly fabricated by welding. Most common are the stainless clad steels which are the subject of a separate review.[5]

The Kelcaloy Joint

The Kelcaloy joint[17, 84] (Fig. 23) which has been developed by the M. W. Kellogg Co. has occasionally been used as transition piece between chromium-molybdenum steel piping and stainless-steel valve sections in main steam piping.

In its essential features the Kelcaloy joint is an electric ingot type of process similar to the intermelting process[5] which is one of the commercial procedures used



A B

Fig. 23 Examples of Kelcaloy joints. (A) Transition piece made for joining chromium-molybdenum alloy-steel piping to a stainless-steel throttle valve. Inset shows plan of uniting the two alloys in a single casting. (B) Stainless-steel turbine throttle valve at right was welded to stainless-steel end of a special transition piece of stainless steel cast on a chromium-molybdenum alloy-steel forging[17]

in the manufacture of stainless clad steels. In the Kelcaloy process, the stainless section is integrally "cast" on the chromium-molybdenum steel forging.

The Kelcaloy joint[79] has been designed to present, within practical limits, an essentially axial bond as a surface of revolution around the pipe axis between the austenitic and ferritic pipe materials instead of the essentially radial bond provided by a butt weld. With the austenitic alloy disposed on the outside and ferritic material on the inside, as presently done, the level of temperature stress is not greatly different from a butt joint, being perhaps somewhat higher. With a dissimilar butt weld there is a plane section involving heat-affected pipe at each end of the weld, in which the ductility and shock and fatigue resistance may be reduced.[79] This reduced effectiveness is more acute on the ferritic side of the dissimilar weld due to metallurgical effects. On butt welds the stresses caused by the differences in thermal expansion of the two pipes and weld metal may easily propagate initial welding defects in this radial zone, since an increase in the defect area proportionally reduces the sound metal area and increases the pressure load stress in the remaining sound metal.[79] On the Kelcaloy joint bond weakness is axial rather than radial, which results in an essential layer construction locally without change in the distribution of the pressure load.[79]

In some applications where thermal fatigue, stress-corrosion cracking, or other embrittlements may occur which are initiated or promoted by high internal stresses this type of joint may offer advantages.

RAILROAD EQUIPMENT

Many dissimilar-metal combinations are made by arc, resistance, and other welding processes in the construction of railroad passenger cars. A typical example is the use of submerged-arc butt welding in joining 14-gage (0.0781-in.) side sheets. High-strength low-alloy steel grades are frequently substituted for a portion of the 18–8 (Cr-Ni) stainless steel side sheets.[22] This type of joint is normally welded with 25–20 (Cr-Ni) wire by maintaining the majority of the weld on the stainless steel side in order to avoid any excessive dilution by the low-alloy steel.[22] Furthermore, a small weld is desirable in order to minimize distortion of the side sheets. It is extremely difficult to place a small weld in the desired location under adverse production conditions of alignment and machine travel.[22] In addition, localized spots of excessive mixing may result from the differences in electrical resistance of the two different steels and from the tendency of the arc to wander at high welding speeds. This condition has been found to produce transverse cold cracks in the martensite at the root of the weld.[22] Insufficient mixing has usually resulted in a high-alloy weld metal with transverse hot cracks in the center of the weld bead.[22]

Maier[22] in experimenting with various commercial stainless steel submerged-arc welding electrode wires produced the most satisfactory weldments with a 29–9 (Cr-Ni) wire analysis. The wire was used with a Linde grade 90 flux containing a minimum of 4% molybdenum as ferromolybdenum. By specifying a maximum silicon content of 0.40% in the filler metal the formation of intergranular silicate films was prevented.*[22] The partially ferritic 29–9 (Cr-Ni) wire analysis and the molybdenum content were additional factors responsible for eliminating hot cracking.[22]

ELECTRICAL EQUIPMENT

Since austenite is non-magnetic, austenitic weld joints in magnetic mild steel components are used in

* The formation of intergranular silicate films in weldments containing over 0.60 to 0.75 % silicon has been associated with increased crack-sensitivity —particularly when the structure is fully austenitic.[5]

switchgear and other electrical equipment to break up magnetic circuits. Tests by Frederick[61] indicate that the thickness of plate material has no more direct bearing on groove width than for normal welding of mild-steel groove welds. Type of electrode, diameter of electrode, welding current, and welding technique are the prime factors, as all four determine the amount of diffusion. Dilution should not occur to such an extent that the austenitic weld deposit becomes ferritic throughout its structure. A sketch of a typical weld test specimen is shown in Fig. 24.

Although 25–20 (Cr-Ni) electrodes seem most suitable, Frederick[61] obtained satisfactory joints with titania-coated 18–8 (Cr-Ni) electrodes. On plate thicknesses of $^{3}/_{16}$ to $^{3}/_{8}$ in. Frederick made square groove welds varying from $^{1}/_{4}$ to $^{3}/_{8}$ in. in width. Since wider square grooves contained slag inclusions, plates having thicknesses above $^{1}/_{4}$ in. had V-grooves with a 20-degree bevel. Different electrode compositions and other welding processes should well modify these relations.

GAS TURBINES

Whereas in steam turbines the rotors are generally forged, in gas turbines the majority of multi-stage rotors are presently fabricated by welding.[35]

Single-stage rotors used in the majority of aircraft gas turbines are generally forged. However, in some single-stage rotor designs of the General Electric Co. and the Allison Co., the ferritic hub has been joined to the austenitic rim by welding.[35]

The center or hub portion which is generally cooled to operate at temperatures below 1000° F. (540° C.) is made of a ferritic alloy steel—usually containing Ni, Cr, and Mo. The rim section, which is generally operated above 1000° F. (540° C.), is made of austenitic stainless [16–25–6 (Cr-Ni-Mo), for example] or other heat-resisting alloy materials. The turbine blades are then joined to the rim section. Since high stress-to-rupture properties are the most important characteristic which the turbine-blade material has to exhibit, highly alloyed compositions are generally used.

According to Montandon[111] the steam turbine rotors produced before the last war by Brown Boveri consisted of several ferritic steel forgings which were welded together with austenitic stainless-steel electrodes. The service temperatures of the welded rotors were in the range of 840 to 900° F. (450 to 480° C.). With a few exceptions where the weld deposits exhibited a high crack sensitivity all turbines were operated without any trouble for many years. Nevertheless, the welding procedure was changed to suitable ferritic alloy-steel electrodes, since they were less susceptible to cracking, and to avoid some of the other difficulties which may be found in dissimilar-metal joints.[111]

In the case of gas turbine parts of turbo-superchargers, where the service temperatures are much higher than in steam turbines, austenitic turbine blades which were welded with austenitic stainless steel electrodes to the ferritic steel disks, exhibited the following difficulties: (1) cracks were present in the austenitic weld metal and (2) failures occurred during the high-temperature service in the ferritic base steel near the bond which could be ascribed to decarburization resulting from carbon migration into the austenitic weld metal. Thus, whenever possible, suitable ferritic stainless steel electrodes have been preferred in these types of turbines.[111]

High-Chromium Alloys

High-chromium alloys have so far not become important commercially.[35] However, a number of welding tests have been performed on a few experimental alloys.

High-chromium blade materials, containing, for example, 60% chromium, 15% iron, and 25% molybdenum, require considerable welding precautions. The major welding difficulties arise from the fact that these high-chromium alloys are readily embrittled by the absorption of nitrogen and oxygen and by the formation of brittle compounds with the nickel which is contained in the electrode.[36] Results of preliminary studies suggest[36] the use of a 60Cr–35Fe–5Mo electrode which is a readily forgeable alloy and which avoids formation of brittle compounds of nickel. Some protective atmosphere may be needed to avoid oxidizing and nitriding reactions during welding.

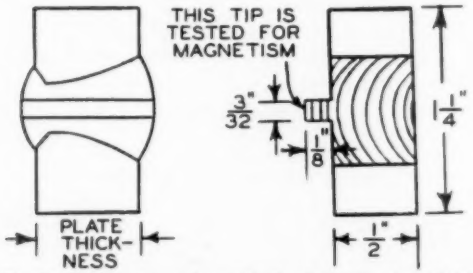


Fig. 24 Weld-test specimen for the determination of the magnetic properties of weld deposits[61]

High-Cobalt Alloys

The high-cobalt alloys do not seem to be readily weldable with austenitic stainless electrodes. Thus bead-on-plate and restrained joint tests with 18–14–Mo (Cr-Ni-Mo)—Type 316—electrodes (electric-arc and submerged-arc welding procedures) showed intergranular weld-metal cracking as well as cracks in the heat-affected zone of Vitallium (29% Cr, 65% Co, and 6% Mo)[11, 27] and other heat-resisting alloys S-816 (45% Co),[11, 14, 27] N-155 (20% Co),[11, 14, 27] and 6059 (34% Co).[27] However, under somewhat less severe welding conditions Evans and Constantine[92] successfully welded

cast Vitallium to carbon steel (SAE 1020) with Type 316 stainless-steel electrodes. A very simple butt-type joint was used about $^1/_2$ in. deep between $1\frac{3}{16}$-in. diameter pieces. A preheat treatment was not required.

The primary difficulty in welding these high-cobalt alloys seems to be related to dilution. Welding processes which keep dilution to a minimum are likely to hold the greatest promise for joining these cobalt-bearing alloys to austenitic stainless steels.

High-Nickel Alloys

The high-nickel alloys appear to exhibit better welding characteristics than the high-chromium and high-cobalt materials. Nevertheless slight weld-metal cracking has been observed in 18–8–Mo (Type 316) weld metal which had been deposited on restraint and interbucket test specimens of Timken 16–25–6 (16% Cr, 25% Ni, and 6% Mo) and Hastelloy B (70% Ni and 30% Mo). Underbead cracking was not observed in these specimens.[11, 14] More recent tests indicate that Hastelloy B can be satisfactorily welded with 18–14–Mo (Cr-Ni-Mo)—Type 316—electrodes. Both the electric-arc and submerged-arc process produced welds free from defects in either the bead-on-plate or the restrained-joint test.[27, 55] The welded joint should be preheated and held at a temperature of about 600° F. (315° C.).[55]

FIELDS FOR FURTHER STUDY

This review of the available published and unpublished information on dissimilar-metal weldments reveals many gaps in our knowledge and the need for many careful investigations. For example, the advantages and disadvantages which may result from pre- and postheat treatment are not clearly understood. Although many specifications require or recommend postheat treatments, the resulting disadvantages often may outweigh the benefits gained. The effects of preheat treatments have also been misunderstood and should be further studied. Similar considerations hold true for peening which, although often beneficial, may also cause detrimental effects.

Dilution should be further investigated. In particular, the effects of various alloying elements merit consideration.

Buttering with 25–20 (Cr-Ni) electrodes is a procedure about which considerable controversy exists. Whereas some investigators feel that buttering prevents or considerably minimizes weld-metal cracking others report that the use of 29–9 (Cr-Ni) electrodes and a standard multipass welding procedure (Fig. 19) is superior to buttering. These controversial reports should be investigated with respect to the effects of section size, amount of restraint, hardenability and composition of base steel, preheating, welding processes and procedure, electrode size, etc.

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(*Continued from page 36-s*)

C., the ordinary room heat, to 100° C., the boiling point of water. The specimens all have the same length, about 2 in., to start with but the inventors have found that as temperatures are stepped up each specimen changes in length in proportion to the amount of stress to which it has been previously subjected.

The apparatus automatically magnifies the amount of each expansion 3500 times and records it. In approximately two hours the total linear expansion of all test specimens has been recorded. When these values are plotted against the stresses previously applied to the specimens, it is found that a sharp dip in the resulting curve appears at the point where the test material reaches its endurance limit.

Tests have already been run on a chemically complex steel alloy supplied by the Allegheny Ludlum Steel Corp., and the endurance limit which was found is admittedly more accurate than that found by ordinary methods.

"Since the Dilastrain Method permits the determination of such important engineering data as endurance limit with the greatest speed and accuracy, it is impossible to exaggerate its importance," said Dr. Gunther Mohling, assistant director of research for Allegheny Ludlum.

Test specimens are now being submitted by other basic industries. The inventors expect to make effective applications of the Dilastrain Method to testing special alloys which are subjected to high temperatures in jet motors and gas turbines.

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JACKSON UNIGOGGLE type W-60, shown above, has plastic headrest, telescopic arms.

JACKSON UNIGOGGLE type WR-60, shown at left, has same eyecup and choice of lens shades, but is held against the face by an adjustable, elastic headband.

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